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The Cover: The Museum of Science and Industry, Chicago, Ill., will be the object of an inspection trip during the coming AIEE summer convention, to be held at Chicago June 22–26, 1942

Kaufmann & Fabry photo

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HIGH LIGHTS ..

The War and the Electrical Engineer. Underlying both technical and general sessions of the recent Northeastern District meeting was the ever-present fact of the war and its impact on the lives and work of electrical engineers in particular. The conversion of electrical engineering to the specialized needs and increased tempo of war production was described by an engineering executive (*pages 283–5*). The role of electrical research in this war was compared with its relatively limited contributions in World War I and an attempt made to evaluate the long-term losses and potential gains of the wartime shift in emphasis (*pages 285–8*). In a talk addressed to the student convention held in connection with the District meeting, some of the war's effects upon the industrial careers of young engineers were considered (*pages 281–2*). Other features of the North Eastern District meeting, described in the keynote address as picturing "the engineer in war and in the peace to come," are reported in the Institute Activities section (*pages 314–16*). The war services of the Institute are discussed in a report on "AIEE Progress" presented by the national secretary (*pages 316–17*). The use of substitutes to conserve critical materials was the theme of a particularly timely conference (*pages 317–20*).

A-C Applications on Warships. The use of alternating current for propelling large naval vessels presented obvious advantages; the more recent change from direct to alternating current for auxiliary power systems, while involving more problems, has justified itself in operation. Some of the major problems were selection of satisfactory operating voltages; introduction of protective devices, voltage regulators, and transformers; adaptation of industrial equipment to naval needs; development of testing and specifications. These and other problems, with the solutions developed, are discussed in detail in this issue and the ship's service equipment on a modern battleship is described (*pages 289–302*).

Transient Conditions in Synchronous Machines. Magnetic saturation in the iron circuits, particularly of the main flux in the rotor poles, substantially determines the performance of synchronous machines in the transient state. By combinations of the voltage–current equations for the electric and magnetic circuits, a rigorous relation has been developed for the change with time of the electromotive force, which can be represented by a graphical construction (*Transactions pages 297–306*).

High-Speed Reclosing. The application of high-speed reclosing circuit breakers to transmission systems has been analyzed for several typical arrangements and conditions, including effect of system inertia, line length, intermediate switching stations, types of faults, fault duration, and deenergization time; data are presented showing the general conditions favorable to various types of reclosing equipment (*Transactions pages 339–48*).

Long-Scale Indicating Instruments. The importance of scales longer than the 90 angular degrees in electrical indicating instruments has been shown by the use of longer scales in nonelectrical instruments and the production of several long-scale electrical instruments in England; a complete group of $4^1/_4$- by $4^1/_4$-inch rectangular instruments with scale lengths of 240 angular degrees has recently been developed (*Transactions pages 318–27*).

Tie-Line Power Control. With the object of determining what characteristics of control equipment are most effective in reducing tie-line power swings during periods of large variable loads, various types of controllers have been analyzed; results indicated that the use of a continuous controller allows a faster rate of correction than can be tolerated with intermittent control (*Transactions pages 306–14*).

Traveling Waves. The extension of operational methods to the solution of transmission-line problems is discussed in the fifth and final article of the series on the application to electrical engineering of advanced methods of mathematical analysis (*pages 302–09*). A pamphlet reprint of the complete series will be available shortly (*page 321*).

Summer Convention. The largest technical program for a summer convention in the history of the Institute will be offered at Chicago, Ill., June 22–26, 1942; other convention plans also are nearing completion (*pages 310–13*). Publication of abstracts of summer-convention papers, begun in the May issue, is completed in this issue (*pages 324–8*).

Voltage Regulator for Aircraft. Important parts of the electric system of aircraft are the voltage regulator and reverse-current cutout, for which basic requirements are the utmost dependability, small size, light weight, and design adapted to manufacture by mass-production methods (*Transactions pages 334–9*).

Man-Power Commission. The program of the recently appointed War Manpower Commission, aimed at assuring "the most effective mobilization and maximum utilization of man power in the prosecution of the war" is presented in the news columns essentially as outlined by Chairman Paul V. McNutt (*pages 332–3*).

Magnetic Field Strength. Series formulas have been derived and arranged so as to provide a complete equipment for computing the magnetic field of a circular solenoid of any shape and at any point, including points within the cross section of the winding (*Transactions pages 327–33*).

Telemeter for Aircraft. A d-c telemeter has been designed with torque, accuracy, size, and weight characteristics that make it particularly suitable for the remote indication of aircraft functions (*Transactions pages 314–17*).

AIEE Prizes. Winners of the AIEE national prizes for papers presented during 1941 are announced in this issue (*page 311*); biographical sketches of the winners are scheduled to appear in the July issue.

Special AIEE Publication. A "Bibliography on Circuit-Interrupting Devices, 1928–1940," has been issued as a special Institute publication, the second technical bibliography thus to be made available (*page 321*).

Sections and Branches. The current annual report on AIEE Section and Branch activities includes statistics on membership, meetings, and attendance (*pages 322–3*).

Coming Soon. Among special articles and technical papers currently in preparation for early publication are: an article on a subject of timely significance and importance by AIEE President D. C. Prince (F'26); parts III and IV of a series of papers on the theory of the brush-shifting a-c motor, dealing respectively with power-factor correction, and speed control with power-factor correction, by A. G. Conrad (M'40), F. Zweig (A'42), and J. G. Clarke (A'41); a paper on the reactance and skin effect of concentric tubular conductors by H. B. Dwight (F'26); a paper on standardized load-center unit substations for low-voltage a-c systems by E. M. Hunter (M'36) and J. C. Page (A'41); a paper on the thermal co-ordination of motors and their associated supply and control equipment on power supplies of 600 volts or less by B. W. Jones (A'11); a paper on high-voltage fusing of transformer banks by H. H. Marsh, Jr. (M'41) and G. B. Dodds (A'29); a paper on high-frequency coaxial-line calculations by H. H. Race (F'39) and C. V. Larrick (A'40); a paper describing temperature aging tests on class A fractional-horsepower motor stators by J. A. Scott (M'34) and B. H. Thompson; a paper on the calorimetric method for determining efficiencies of electric machines by Victor Siegfried (M'38) and C. W. Thulin (A'42); a paper on rectifier terminology and circuit analysis by C. H. Willis (F'42) and C. C. Herskind (M'40); and a paper on selenium rectifiers and their design by J. E. Yarmack (A'35).

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The Effect of War on Young Engineers Inducted Into Industry

M. M. BORING

Students and young graduate engineers should find food for thought in these remarks from an old friend and competent adviser.

THE WAR is bringing to engineers and other college people a completely new set of experiences. The colleges themselves will feel the most immediate effects, and these are already indicated by the much-discussed speed-up plans, and by the plans for inducting young men into the Navy, the Signal Corps, and other branches of the military service. The trend in college education indicates that a large percentage of those young people who normally would come into industry in the next few years will be diverted into military channels, and a decreasing number will be available to industry.

Only a few years ago it was stated that the colleges were producing too many engineers. Many of the schools were encouraged to, and did, reduce the number of men graduating. We now find a complete reversal of the situation, with a shortage of technically trained people that is estimated, from some sources, as high as 100,000. In addition to this extreme shortage of men in the industrial world, there is the tremendous demand for men in the Army and Navy.

It has been stated* by Dean J. W. Barker of Columbia University that during the next few years the Navy alone hopes to obtain as many as 80,000 technically trained men per year through the colleges. This number is approximately equal to the average annual number of graduates of all the colleges of the United States in recent years.

Because of this, and because of the extreme increase in production, the manufacturers, public utilities, and other organizations which employ the engineering graduate, have been forced to change many of their policies. The immediate effect on the young engineer as he comes to the industrial organization will be a drastic foreshortening of the normal training period; he will be given less guidance and less opportunity to grow slowly and naturally. To meet the crucial time limits of war demands for industrial production, the normal procedure for steps of individual advancement as skill and experience are acquired must be set aside "for the duration," and men must be thrown into positions of advanced responsibility with minimum preparation. This will mean to the individual young engineer increased responsibilities—probably in many cases well beyond his real ability—and it will certainly mean that he will be forced to work harder and under conditions far less favorable than he has before experienced. The tremendous changeover from peacetime equipment to war production, with its restrictions, shortage of raw materials, lack of machine tools, and other equipment, is certainly going to tax the ingenuity of young men as never before. Unfortunately these abnormal conditions—acting as they do to thrust young men so quickly into positions of greatly advanced responsibility, and in many instances giving men just out of college an income far above the level that could be expected under normal conditions—are apt to give the young men graduating today a false sense of values. It is essential for young men to remember that this is a transient condition from which a return to normal is inevitable. The far-seeing young man will prepare against this inevitable future readjustment on the one hand by making the most of his enhanced opportunities to acquire skill, judgment, and experience, and on the other hand by conserving his currently abnormal remuneration to assure buying power later on.

Many of the young graduates have developed a jittery attitude and question whether or not the particular small bit they can do in industry toward the war effort is as useful as their services would be if they were actually carrying guns. The shortage of men in the industrial field, therefore, is increased because many young men decide that they should enlist in some branch of the armed service, instead of taking industrial jobs.

We older people should advise young men to go slowly in making decisions of this kind. It has been stated that it is necessary to maintain 14 people in the production of war goods in order to keep one man in the field. In the last war this requirement was estimated at $2^1/_2$ people, which indicates the comparative technical changes in the last 25 years. If we build up an army involving several million men, as we expect to, it will be necessary for industry to accumulate 50 to 60 million more workers than we now have, and all of the war material that must be created by this tremendous number of people is so technical in nature that our Army will be unable to function without having in industry a high

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Essential substance of a talk given before a luncheon gathering of the student convention held May 1, 1942, in conjunction with the North Eastern District meeting at Schenectady, N. Y.

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* *Editor's Note:* See "Navy Announces Training Program in Collaboration With Colleges," *Electrical Engineering*, April 1942, pages 218–19, which explains the Navy's hopeful plans to encourage properly qualified young men between the ages of 17 and 19 years to enter (or remain in) college as apprentice seamen while training for advance ranks.

percentage of technically trained men who can use their engineering talent for this purpose.

Various representatives of the armed forces have recently been approaching students in colleges and young engineers in industry with proposals that these men should immediately join the armed forces. Some of these statements seem to be diametrically opposite to the thinking of the Selective Service. It would certainly be unwise for the armed forces to draw from important war industries highly trained technical people whose entire effort is directed toward the production, development, design, testing, or manufacture of devices badly needed by the Army and the Navy. These industries, which have turned over their complete manufacturing force to the war effort, today cannot in any sense of the word be considered as private industries, but are now definitely government arsenals. In support of this view, an official of the Selective Service recently stated that taking men from industries of this type would be analogous to taking away their raw materials and yet requesting that they produce more finished products.

Professors, on the one hand, have been advised to tell students that the chances of getting more than one six-months' deferment were practically nil. On the other hand, instructions to Draft Boards have clearly stated that young men actively engaged in the production of war goods will be granted additional requests for deferment, as long as they are vitally needed by the war-production effort. Also, many colleges have been instructed to write to the electrical-engineering graduates of the last 10 years, encouraging them to leave their present war-production jobs to take special courses in ultrahigh frequency phenomena, with the idea of working toward commissions in the Signal Corps. Surely, under the present conditions, it would be most unfortunate if there was a large exodus of engineers from war industries under this sort of plan.

So our advice to young men should be, at least now when production is needed so badly, to enter industry. Later, when the right time comes, when there is a good flow of production, and when the creative thinking of the engineer is not needed so badly to produce war goods, then you, as young engineers, should be ready to take your abilities to the armed forces, to use, and to teach the layman how to use, the things that you have built. In other words, we should say to you that you should keep your heads, use your common sense, and make the same type of analytical study of this war problem as you would of an engineering problem.

As to the future, older people who have been involved in the production of materials are leaving to you a most unusual heritage. We are going to leave you a world torn by war, in which we shall expect you to correct the social structure and the economic structure, as well as to rebuild the physical things. Not only will you be responsible for the physical rebuilding of Europe, and for paying for that job, but also for finding a way to create in European countries the same standard of living that we in the United States enjoy, so that those jealousies that eventually break out into actual wars will be eliminated. You must take a larger part in the economic and social phases of our civilization than engineers have ever taken before.

I have found from my experiences in contacting young men that most of them seem to think that their entry into industry will be only temporary, and that a complete collapse in the industrial world will come at the end of the war effort. Yet history shows that even though there may be a short, deep depression immediately after the war, caused by the readjustment necessary in changing from building of war material to producing goods for peace, this should be followed by a long period of high productivity.

In addition to the rebuilding necessary because of the destruction of cities, factories, transportation, and other necessities, you will be faced, as no engineers have been before, with the problem of catching up with the building of the materials that have been necessarily neglected. This obsolescence, caused by the war and by the long depression that preceded the present conflict, will be at the greatest point in history. The fact that there has been practically no building of homes, heavy transportation facilities, or power-generating equipment for nearly ten years indicates that there will be great shortages facing us immediately after the war is over.

After all, regardless of wars or of depressions, young men grow at a fairly uniform rate. It has been my experience that every job has certain changes in it about every five to ten years, so that at least part of its responsibilities are taken by a new individual. Under these conditions, we must realize that there is a fairly rapid turnover of men in all jobs—administrative, technical, and skilled—so that you as young people will, when the time comes, have the complete responsibility of running industry, and your only competition will be with others of your own age. You will not compete with older or with younger people. When the time comes, you can be perfectly sure that some of you will hold high executive positions in the great manufacturing companies, that all of the older people will make way for younger men, and that you will have unquestionably the greatest opportunity in history for the reorganization of our civilization.

Undoubtedly we must all expect some decline in our standards of living, but surely the American people are not ready to give up permanently the motor cars, the radios, and the refrigerators that we have enjoyed in the past. In some way it will again be possible for the man who builds the automobile to buy the radio, the man who builds the radio to purchase the refrigerator, and so on. Since this is so, I believe we can assure you that the period of rebuilding following this war effort will be an extremely interesting one, that you will have a job to do, that you will have plenty of responsibility, and that you will have grand fun doing that job.

Wartime Electrical Engineering

R. C. MUIR
FELLOW AIEE

Applying his accumulated knowledge and skill, the electrical engineer is "meeting the war assignment admirably," successfully and rapidly shifting his efforts from peacetime problems to supply the specialized needs of war.

THE ELECTRICAL engineer has been preparing for this war for 20 years or more, although he may not have been aware of it. He has been rapidly gaining knowledge in practically every branch of electrical engineering since World War I; and as this knowledge accumulated through research, experiment, and analysis, he has interpreted it into machines, devices, systems, and practices for peacetime or civilian use. Since early in the emergency period preceding the present war, his efforts have been shifted to the adaptation and application of this knowledge to war machines and practices.

This does not mean, however, that wartime electrical engineering is just a continuation of what the electrical engineer had been doing. The tempo has increased. The time factor has become much more important. Economic consideration has been eliminated in the ordinary sense, in that winning the war is a *must* proposition and markets are assured. Research and developments now proceed precipitously, and without the usual analysis of whether or not they will pay out in the usual business sense.

It has become apparent that this must be a war of production, because the strategy of the armed forces of the United Nations is vitally dependent upon having the tools of war available at the time and place they are needed, in sufficient quantities, and of a type equal to or superior to those of the enemy.

In a management engineering position, it is very difficult to confine oneself to a consideration of engineering as such. I find myself watching the production figures day by day on products I know are vital to the prosecution of the war. It is hard to be patient with any delays in completing designs and engineering and manufacturing instructions, even though one knows there must be difficulties with entirely new devices such as are required. Those in management positions not concerned with engineering are inclined to take the engineering for granted, overlooking the fact that before production can begin the product must be designed and tested and proved suitable for the job it must do.

It has been said that we can design an equipment to perform almost any function if we resort to enough complication and employ an unlimited number of gadgets or devices. However, since this is a war of production, the engineer must give more consideration than ever before to conserving materials, man-hours, and machine-hours. Today, machines and equipment for even the most difficult jobs must be of simple design so that they can be manufactured readily and in sufficient quantities without bogging down our facilities.

This increased tempo and the time factor have affected all branches of electrical engineering. Schools must turn out their students earlier, must specialize to a greater degree, and must carry on emergency training for large numbers in specialized courses. The electrical engineer in the electric-power companies has had to expand the power systems rapidly to serve industry; and the operating electrical engineer in manufacturing plants has found it necessary to plan and install much new equipment to produce the materials of war.

The shift in manufacturing from civilian products to war products has demanded a corresponding shift in electrical engineering and has disturbed the balance of effort previously applied to engineering products.

It has been said that radio was born during World War I, but in the present war, radio in all its various branches has become an essential part of every military or naval operation. During the past 20 years a vast fund of knowledge has been accumulated in the electronics field: on frequency modulation, television, high-frequency phenomena. New tubes and circuits have come into being rapidly and have made these things possible, but one might say that up to the present war period we have really been playing with them. Suddenly the war comes upon us and we realize that radio offers possibilities in the way of providing military information which were almost beyond our imagination a few years ago, and that these radio devices are required in many varieties and in tremendous quantities. There is a decided shift, then, of electrical engineering and research in the direction of radio and electronics.

In peacetime very little of the electrical engineer's effort was required for ordnance, but the war suddenly thrust upon him a wide variety of intricate problems in ordnance control. Here there is not only a shift of effort, but in addition the development of a kind of engineering with which but few were familiar—that involving electric machines and circuits co-ordinated with highly complex mechanical mechanisms, optical systems, and radio.

Essential substance of an address presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942.

R. C. Muir is a vice-president, General Electric Company, Schenectady, N. Y.

naval and army applications. We have been learning more and more about heat transfer, and this knowledge is extremely valuable in the design and installation of most engineering products for war purposes. Metallurgy and chemistry have given us a number of materials with properties greatly superior for our purposes to those formerly available.

The world has been becoming more measurement-minded and now we find the accelerated work on measurement instruments and methods of great value in wartime engineering. Extensive work on the development of electronic devices has already been mentioned. Extensive work also has been done in applying such devices to aid in the manufacture of other products, many of which are themselves now vital to the war program. We might enumerate many such gains from the experience and work of the past two decades that have facilitated the shift of electrical-engineering effort from peacetime to wartime.

Despite the fact that we are all out on a wartime basis, one cannot entirely resist giving a thought to the future. The experience of peacetime engineering was invaluable for war purposes. When peace comes, shall we find that our wartime engineering will be of great value in keeping our people employed and in maintaining our high rate of industrial activity? It seems so.

Already there are many developments that promise to have peacetime application, although it is entirely too early to prophesy in any great detail. One finds that ships, airplanes, tanks, searchlights, guns, and other implements of war require control that must co-ordinate to a high degree of accuracy the motions, directions, or indications of two or more devices that are separated from each other. Corrections may be introduced in certain of these controls so that correspondence makes an allowance for a number of variables. The electrical engineer has been so successful in providing these exacting control requirements through electrical means that the electrical method is becoming widely used. The postwar period should provide great possibilities for this type of control in manufacture and transportation.

General Electric photo

In the foreground are sections of generators for a hydroelectric power project; stators for 6,000-horsepower ship-propulsion motors are in the background

The airplane has introduced innumerable problems for the electrical engineer, which might be summarized as an attempt to provide the airplane with all the operating and armament devices required for a battleship. Instruments, generators, motors, complicated control systems, armament and ignition systems must be of extremely light weight and designed to withstand severe vibration and to operate from sea level to high altitudes under widely varying conditions of humidity and temperature. This represents an entirely new field of electrical engineering, and there has been a decided shift of effort also in this direction.

Modern naval vessels have brought about many new problems for the electrical engineer. Since World War I the electrification of a warship has become much more complete and complicated. Automatic controls, flameproof cable, shock-proof mounting, mine protection, gun laying, remote-controlled searchlights, radio communication, signaling, and detection, and many other requirements have shifted more effort in this direction.

The basic work of the past 20 years has provided the engineer with tools which serve him well in this wartime emergency. For example, we have been becoming more noise-conscious for several years, and what we have learned about noise prevention becomes of great importance in electric equipment for submarine and other

Also, new things may be born, as was radio in the last war, for engineering puts to use new additions to our accumulation of scientific knowledge. Under the stress and stimulation of war new knowledge comes fast, so our accumulation is growing rapidly and will be available to peacetime products. I cannot bring myself to give any more than this hurried glance to the future, however, because all these gains will come to naught unless we win the war and this demands our entire effort for the present.

One might ask—how has the electrical-engineering profession met this war assignment? I would say, admirably. Each engineer has been extremely anxious to make his contribution, so the shift, from the organizational point of view, has been natural and easy. If there ever has been a sharp division between electrical and mechanical engineering, it has been quite effectively broken down, so that there has been no delay on problems requiring a high degree of both, and most of the war problems are of that kind. Close co-ordination of engineers and research workers has shortened the time required to incorporate the new knowledge of science into engineering products.

This desire to contribute, this close co-ordination of different types of engineering and research, and the excellent groundwork in science and engineering laid during the past 20 years, and the wartime stimulation of the inherent ingenuity and resourcefulness of the engineer have resulted in successful solutions of most difficult problems with remarkable promptness.

This is an all-out war in which engineering is a vital factor. We have said that this is a war of production, but even though we attain an advantage in production, we might lose unless we maintain technical superiority. We must do more than merely match the engineering of our enemies, who have been applying themselves to military preparation much longer than we have. Thus far we have done well, but we are now entering the second stage—a period of intensive research, invention, reduction to practice, and refinement of design. We must apply ourselves to the task with increased spirit and vigor, and exercise with all our ability our traditional co-operation, ingenuity, and resourcefulness.

Our research, engineering, and manufacturing must be much more closely correlated than formerly. There must be overlapping of these three functions, so that the new things developed through research and engineering may be incorporated without delay in the products leaving our factories. The electrical engineer must assume his part of the responsibility of giving our fighting forces at the battle front equipment technically superior to that of the enemy. This is wartime electrical engineering.

Wartime Electrical Research

L. A. HAWKINS
MEMBER AIEE

Government encouragement, more laboratories, and many more trained workers characterize the conditions for research in World War II in comparison with those of World War I. Experience with the relatively limited research of that conflict, however, justifies the hope that the present effort, in addition to its primary purpose of contributing to victory, will result in now-unpredictable developments of great peacetime value.

ALTHOUGH the United States has been formally at war for only a few months, fortunately wartime research in the United States began more than a year and a half ago.

In the summer of 1940, following the declaration of a national emergency by President Roosevelt, and his appointment of the National Defense Research Committee, programs were formulated and work begun. Then, with the passage in March 1941 of the Lend-Lease Act, which in effect pooled our resources with those of the Allied Nations, there came a full interchange of technical information, which stimulated and expedited American research, by bringing a clarification of the problems and full knowledge of what the British had already accomplished toward their solution.

Now our research programs are in full swing. Literally thousands of scientists are intensively engaged on the multifarious technical problems of this highly mechanized war, and they are backed by ample funds.

Of this wartime research, a very large part is electrical. President James B. Conant of Harvard University has

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Essential substance of an address presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942.

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said truly that this is a physicist's war. And just as electrical phenomena have, in the past 25 years, taken to themselves a greatly increased portion of the science of physics, so electrical engineering, which deals with the applications of those phenomena, has assumed a far more important role in warfare than ever before, so that this war might justly be called also an electrical engineer's war.

The diversity of the contributions electrical engineers are making to the war activities is indicated by another article ("Wartime Electrical Engineering," pages 283–5). The task for electrical research is to seek new facts and to open up new fields, to enable the engineers to diversify their contributions still further and to make them still more effective.

Those who were engaged in war research in World War I must be impressed by certain differences in the conditions for research then and now. To me that difference seems somewhat analogous to that between the conditions of warfare in colonial days and in modern times. In the fights of the French and Indian wars, our forces relied, not on the mass movements of well-drilled troops, but rather on the initiative, woodcraft, and marksmanship of each fighting man. There must have been in those days a sense of freedom and a thrill of individual achievement which is perforce largely lacking in modern mass warfare, in which the individual is a minute part of a vast and complicated machine.

And so it was to a great extent with war research in 1917. There seemed to be little understanding of, or interest in research on the part of the military, or elsewhere in Washington, with the exception of the office of the Secretary of the Navy, Josephus Daniels. It is interesting to remember that the Assistant Secretary at that time was one Franklin D. Roosevelt. Daniels appointed the Naval Consulting Board, with Thomas A. Edison as chairman, and in many ways encouraged and assisted research on naval problems. Such encouragement and help were sometimes badly needed, for with certain striking exceptions the high-ranking officers in the Navy in 1917 seemed to think it absurd to expect anything of value in naval warfare to come from any civilian or group of civilians.

In consequence, much of the war research in the opening months of the war had to be at the initiative and expense of the relatively few industrial research laboratories then in existence. That meant complete freedom in choice of projects and in the planning and execution of research programs. The result was a sense of individual initiative and achievement, the latter being favored by the fact that the number of research groups was relatively small. Difficulties sometimes arose in obtaining sufficiently comprehensive knowledge of details of the military problems and an adequate tryout of research results, but nevertheless the wartime researches of World War I contributed very materially to the final victory, and the output per man and per dollar was high.

Conditions now are very different. There is ample interest in research in Washington, shown by a willingness to back research with generous, almost prodigal, appropriations. Mass attack is the order of the day. The light mobile guerrilla bands of 1917 have been replaced by much larger groups, better equipped, more fully organized, with more centralized supervision and control. Some war-research workers may at times sigh for the "good old days," when initiative was more free and mobility higher, but it is obvious that the enormous increase in number and complexity of the scientific problems of modern war demands an increased degree of centralized control, if wasteful duplication is to be avoided and each problem is to receive the attention which its urgency demands.

Another very great difference between now and 1917 is the far greater number of research men available. Twenty-five years ago the number of industrial research laboratories was small. Today, practically every industrial company of any size has its research laboratory, and as those companies have been turning to war work, so their laboratories have been attacking, on their own initiative, the technical problems involved in their particular field of war production. This means a tremendous accretion of war research. Better materials, better designs, and new developments are pouring forth in a growing stream, and, in the electrical field, the industrial research laboratories are doing their full share.

The work of the industrial laboratories in their special fields is being supplemented by large research groups, drawn mainly from the universities, who are pioneering in new fields that offer a prospect of radically new developments of war value. Here again the number of available workers is far larger than in 1917, for in the past 25 years the quantity and quality of the research work in our colleges has increased enormously, with a corresponding increase in the number of able and highly trained research men.

Another striking difference between research conditions in the two world wars is the very much greater part which British physicists are now playing. In World War I their contributions were relatively small. For instance, when the United States entered that war in April 1917, the Allies had no submarine detectors worthy of the name. Through the intensive co-operative effort of three industrial companies in the United States, working on their own initiative and at their own expense, two successful types of detector were completely developed and ready for quantity production by the fall of that year. Samples were taken to England in November, and in the first week of January 1918, the first submarine hunt in history was begun and brought to a triumphant conclusion by bagging a submarine on the third day. These detectors were at once adopted by the British Admiralty, and a little less promptly by the United States Navy. How effective they were, under the conditions then existing, is shown by a comparison of the rates of sinkings by submarines in the spring and fall of 1918. In the spring, the rate of sinkings reached its peak. Before the armistice, the submarine had been reduced from a major to a decidedly minor menace.

In the late spring of 1918, during a visit by a commission from the British Admiralty, I was asked by one of the officers why I thought it was that the United States had accomplished more in six months than they had in three years, although they had some of the ablest physicists in the world working on the antisubmarine problem. I was glad when he went on to suggest that it might be because their scientists were university men who had had little contact with, or interest in, or respect for industry and did not even talk the same language as the industrial engineers, nor enjoy their respect, while in the United States we had had scientists, engineers, and production men trained by years of close co-operation to work together as a team. I told him I thought he had answered his own question.

General Electric photo

A 75-millimeter pack howitzer tube being inspected with a borescope, a mirror at the far end of which reflects an image of the surface into a microscope

In this war the story is very different. When, with the passage of the Lend-Lease Act, we were released from the strait jacket of helpless and stultifying neutrality in which we had so stupidly bound ourselves, it became possible to learn what British scientists and engineers had already accomplished toward the solution of some of the most difficult technical problems of this war, and we were filled with admiration. Just as British purchases of war materials gave American industry a start in war production, so British technical developments have given American research a big boost toward overtaking the long lead gained by Germany and Japan in their all-out concentration on war problems for many years.

Now both American war industry and American war research are well under way and are gaining speed, and just as our industries will, in the near future, overtake in output the combined war industries of Germany and Japan, giving us, in combination with the British Empire and Russia, a clear superiority in all the weapons and other implements of war, so, we may feel confident, the combined brains of the United Nations in war researches will overtake and surpass the best that the Axis powers can do, even with their long headstart.

For such success, in both industry and research, there must be full mobilization of resources, and sustained, intensive, unselfish effort. In both, the mobilization has already been largely accomplished, and in both, we are confident, the effort will be forthcoming. Indeed no group in the United States was quicker than were the scientists to recognize the extent and gravity of the menace to our liberty embodied in Naziism. In December 1938, nine months before Hitler precipitated World War II by his invasion of Poland, and a year and a half before President Roosevelt proclaimed a national emergency, 1,284 American scientists joined in a manifesto, which replied to a statement of official Nazi position on science and scientific research, and which contained these significant words:

"We firmly believe that in the present historical epoch democracy alone can preserve intellectual freedom. Any attack upon freedom in one sphere . . . is in effect an attack on democracy itself."

If the rest of the American public and its leaders had been as clear-sighted as those 1,284 scientists, we would have been far better prepared for war, victory would have been far less remote, and many American lives would have been saved.

Scientists know how vital to the spirit of science is freedom—freedom of thought, of inquiry, of speech. They are sickened by the spectacle of the once-proud

German science prostituting itself to ruthless policies of statecraft, and slavishly echoing acquiescence to doctrines well known to be false. Such a demonstration of the fate of science under totalitarianism makes them glad to sacrifice for a time their special interests, and to accept such temporary regimentation as may be needed for all-out organized offense against the monstrous menace.

It is no small sacrifice for many a scientist. To turn from the alluring search for truth for truth's sake and for the good of mankind, to the development of implements of destruction, may mean a painful dislocation of deep-seated incentives. But sacrifice is the common lot of all in war, and the scientist has a special compensation in his knowledge that the war effort, beyond its primary and all-important purpose of contributing to victory over the enemy, in all likelihood will yield things of great value when peace returns.

Not only will many of the new plastics, alloys, synthetic rubbers, and other materials developed under the pressure of war needs find peacetime uses, not only does the opening through war projects of new fields such as that of radio microwaves reveal vistas of important peacetime developments, but new things now unpredictable will spring from the war effort.

Experience in the years following World War I justifies hopes of such a fortunate outcome. For instance, when, in that war, research workers developed a portable X-ray outfit for use in the field hospitals in France, we did not foresee either the tremendous stimulus that would be given thereby to the use of X rays by the medical profession, nor the revolutionary peacetime developments that would flow from it, starting with the oil-immersed dental outfit, which made the use of X rays by the dentist safe, convenient, and practically universal, proceeding through larger similar outfits for diagnostic and therapeutic use in hospitals, and culminating in the million-volt industrial X-ray generator, now giving invaluable service in the present war effort of the heavy industries, and acting as a tremendous stimulus to the industrial use of X rays hereafter.

Nor, when in 1917 we were designing, developing, and manufacturing transmitting tubes for use in that novel communication method called wireless telephony, of which our armed forces wished to avail themselves, did we foresee that we were shaping the cornerstone for the central building of a great new industry—the radio-broadcasting station.

Indeed, the new problems and the new approaches to old problems, forced on us by the exigencies of war, contain in themselves compensation for a disadvantageous change, similarly enforced, from the research aims in peace. In normal times, it is the more fundamental researches which yield the more radically new developments. But such researches always, from their very nature, lack definite practical objectives, and often must extend over many years. Both characteristics tend to inhibit fundamental research in wartime. There are too many definite objectives calling urgently for immediate attainment to permit of discursive pioneering, and quick results are worth infinitely more than results indefinitely postponed.

With most fundamental research laid aside for the duration of the war, the yield of radically new things would greatly diminish, were it not that the attainment of the war objectives often forces research into wholly new fields and provides new viewpoints toward old fields. Applied research in war therefore may contain greater potentialities for revolutionary developments than are often found in the applied research of peacetime. Postwar planning may therefore find itself unexpectedly reinforced by developments, now unforeseeable, springing from war research.

But meanwhile we have a job to do, an all-important job for research men and engineers alike. If we fail in that job, postwar plans and the whole national economy of the United States, as well as all our liberties and free institutions, will have gone with the wind. We must not fail, and I am confident we shall not fail. We must win through to victory, and, in attaining it, I am confident that again, as in World War I, research will do its part.

But how about the next world war? We have compared research in World Wars I and II, and have seen the far greater number and complexity of the problems now confronting us. We have seen the number of scientists engaged in war research increased from hundreds to thousands, and their expenditures from millions to tens of millions of dollars. Are we now to look forward to the organization, in another 25 years, of tens of thousands of scientists, spending hundreds of millions of dollars, to attack the still more numerous problems of still more mechanized and still more terrible war? Or this time shall we finish the job?

When victory is won, and the consequent emotional letdown brings war-weariness sweeping over us in waves, shall we again permit our isolationists to lead the United States back into the primrose paths of so-called "normalcy," leaving the rest of the world in an international chaos to breed still more sanguinary conflicts? Shall we again pour out our blood and treasure to "make the world safe for democracy," and then desert the task just when we have won the power to complete it? Or shall we finish the job, joining with the other free nations in intensive, sustained, and unselfish co-operation, to erect upon the field of victory, around the cornerstone of the Atlantic Charter, a new world structure, to which all nations of good will may resort, to find liberty, security, and opportunity, and which will be so strong that no gangster nation will dare attack it? Are we to go down in history as the generation which twice had offered to it the greatest opportunity in the history of mankind, and twice refused it in selfish stupidity, or are we to be recorded as the generation which fought and won the world's last and greatest war and established the world's first enduring peace?

Alternating Current in the United States Navy

H. G. RICKOVER

Official U. S. Navy photo

Although alternating current has been used for propulsion on large United States Navy vessels since 1913, its first use for auxiliary apparatus and lighting on ships was in 1932. The problems involved in introducing alternating current for that purpose on new vessels are discussed, and some of the principal a-c apparatus described, in this general survey of naval applications of alternating current.

ELECTRICITY was first applied on a vessel in the United States Navy in 1883 when the cruiser *Trenton* was equipped with an Edison shunt-wound 13.2-kw 110-volt d-c generator for lighting purposes. From this very modest initial installation the use of electricity has increased to the point where a modern battleship has as much as 10,000 kw of generating capacity for auxiliary operation and lighting, and the largest aircraft carriers have generating plants of 160,000 kva for propulsion.

It is not the purpose of this discussion to give a detailed history of the development and application of all the various types of electric apparatus now in use in the Navy. Some major developments, however, such as the application of alternating current to ship propulsion and the more recent adoption of alternating current for ship's auxiliary and lighting power are described in some detail. The intention is to outline the principal reasons for adopting alternating current for its present various applications, the problems involved in changing over ship's service from direct current to alternating current, and the solution of these problems, and to present a general description of the principal apparatus involved. The more important electrical features of some vessels currently under construction are given. Since submarines depend upon storage batteries for a power supply when submerged, they are equipped, except for minor

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applications, with d-c apparatus and therefore are excluded from this discussion.

A-C PROPULSION ON NAVAL VESSELS

The first major application of alternating current in the United States Navy was made in 1913 on the twin-screw collier *Jupiter*. This was an experimental installation and consisted of a single turbine-driven alternator which supplied power to wound-rotor induction motors, one on each shaft. Resistance inserted in the secondary of the motors provided speed variation and also enabled them to meet the severe torque requirements of propeller reversal. Extensive tests were conducted on this installation and it served as the proving ground for future applications. This vessel was later converted into an aircraft tender and the propulsion equipment continued to operate satisfactorily.

The success of this installation resulted in the adoption of a-c propulsion for the battleship *New Mexico*, which was placed in service in 1918. Two turbine-driven generators supplied power to four double squirrel-cage induction motors. The motors were provided with a pole-changing winding and the generators with a voltage-changing winding which were adapted to each other in such a way as to give three working combinations corresponding to 8,000, 16,000, and 32,000 horsepower. In each of these working connections, the motor as well as the generator operated at its maximum electrical efficiency, and thus avoided the reduced efficiency which ordinarily accompanies the operation of electric-power apparatus at low output.

From 1920 to 1923, five more electrically propelled battleships were built. Until 1941 they were our most modern battleships. These five vessels are very similar in size, armament, armor, and other respects. A brief description of their propulsion equipment follows.

The *Tennessee* and *Colorado* have two turbine-driven generators, each of which is capable of delivering a maximum of 15,000 kva at approximately 2,200 rpm. Each of the four propellers is driven by a two-speed wound-rotor induction motor. Pole changing provides two definite speeds, and further speed variation is obtained by throttling the steam turbines. Starting and maneuvering are accomplished on the high-speed winding by inserting resistance in the motor secondaries. The low-speed winding is used only for cruising operations. Each motor delivers approximately 7,300 horsepower at 170 rpm, which is the maximum propeller speed.

The *California*, *Maryland*, and *West Virginia* each likewise have two turbine-driven generators and four induction motors, each driving a propeller. The induction motors have a single stator winding which is externally connected for either 24- or 36-pole operation and the rotors are provided with two separate windings placed in the same slot. The inner rotor winding is a high-resistance squirrel-cage type, which is active only when starting or reversing. The winding nearest the air gap is of low resistance and is phase-wound. It may be operated either open or closed by means of suitable contactors and collector rings.

In the year 1927 the completion of the large aircraft carriers *Lexington* and *Saratoga* resulted in the most completely electrically operated large warships ever built. These ships were originally planned as battle cruisers, but in accordance with the Washington naval treaty were redesigned and completed as aircraft carriers. The propulsion machinery is essentially the same as that used on former vessels, although it differs slightly in some details. The changes were caused by the relative magnitude of power involved in these vessels as compared with previous electric-drive installations, as well as by the general development of the electrical industry.

The propulsion equipment on these two vessels consists essentially of four 40,000-kva adjustable-frequency turbine generators connected through suitable switchgear to eight 22,500-horsepower induction-type propelling motors. The motors are arranged in pairs, each pair being located in a separate compartment and directly connected in tandem to its propeller shaft. The stators are wound for either 22- or 44-pole operation and the rotors have both high-resistance squirrel-cage and phase-type windings. The system of control provides for a varied number of combinations of motor and generator connections to suit the speed and power requirements of the vessels from cruising speeds to full power, including proper facilities for maneuvering the vessel as may ordinarily be required. When operating at full power, each main generator is connected independently and directly to two motors operating in tandem on a single shaft. The propeller speeds are changed by varying the speeds of the respective turbine-generator sets. Additional variation is accomplished by reconnecting the driving motors for either of two speeds. This arrangement provides a very flexible and reliable means of propulsion.

On trial tests the *Lexington* made a 2,228-mile run from San Pedro, Calif., to Honolulu in 72 hours and 34 minutes, resulting in an average speed of 30.7 knots which broke all existing sustained speed records. As a further display of versatility, the *Lexington* later served the city of Tacoma, Wash., as a floating central generating station for a period of 30 days. This emergency was created by lack of water for the hydraulic stations which were the chief source of power for the city.

These installations constitute the electrically propelled combatant vessels in operation in the United States Navy in 1941. All heavy craft now under construction are being equipped with turbine-gear drive.

SHIP'S POWER SUPPLY FOR AUXILIARY APPARATUS AND LIGHTING

Prior to 1932, all United States naval vessels employed direct current for electrically driven auxiliaries and lighting. Alternating current was used only for a few specific applications such as interior communication, fire control circuits, and radio equipment, and was obtained from motor-generator sets fed from the d-c supply. At this time the only large applications of alternating current in the Navy were the main propulsion systems on the battleships and aircraft carriers just described. Even with these huge installations of alternating current for propulsion, direct current was still used for auxiliaries and for lighting.

It was recognized that the adoption of alternating current for ship's power offered distinct advantages but at the same time the changeover involved problems that would require careful analysis. Dependability is the foremost requirement of naval equipment and a warship is not the place for unproved or experimental apparatus.

Some of the advantages of the a-c system as applied to the various types of equipment are:

1. Motors and generators.
(*a*). Increased reliability.
(*b*). Decreased maintenance.
(*c*). Reduction in weight, space, cost, and spare parts.

2. Motor control equipment.
(*a*). Less complicated.
(*b*). Reduction in weight, space, and cost.

3. Cables.
(*a*). Improved copper efficiency, particularly if higher voltages are used.

4. All types of equipment.
(*a*). In case of emergency it is easier to obtain equipment which is more nearly standard commercial design.
(*b*). The probability of improvement in design in future years is greater due to the extensive commercial use of alternating current.

Some of the more important problems associated with a changeover of this nature are as follows:

1. It was evident that in order to derive the full advantages of alternating current a voltage higher than 115 or 230 volts would be desirable. The selection of the proper voltage involved consideration of generator design, current-carrying capacity of switching equipment and cables, as well as insulation and hazard to personnel.

2. A-c systems are subject to high short-circuit currents during faults and disturbances. Suitable circuit breakers, protective devices, and operating relays would be necessary.

3. The older d-c systems had operated satisfactorily without voltage regulators. The a-c generators would require some form of automatic voltage regulator.

4. Additional meters and instrument transformers would be required for alternating current.

5. The problem of power factor was introduced. This would require careful planning of distribution and grouping of motors, particularly with regard to starting currents.

6. If the voltage adopted was 230 volts or higher, transformers would be required for lighting and other low-voltage applications.

7. Some form of reduced-voltage starting would be required for the largest squirrel-cage induction motors.

8. Either multispeed squirrel-cage or wound-rotor induction motors would be required for a few applications where speed variation was necessary.

9. Some d-c power would still be necessary for excitation, searchlights, and minor applications. A suitable method of obtaining this would have to be provided.

10. Emergency power from batteries was suitable for the d-c systems. Some other means of supplying emergency alternating current would be necessary.

11. The engineering and operating personnel were accustomed to d-c machinery. An educational program covering the new apparatus would be required.

12. Complete tests of this new equipment to determine suitability would have to be made. Likewise, applicable naval specifications would have to be prepared.

In 1932 it was decided that a class of destroyers commencing with the *Farragut* would be equipped with alternating current for ship's service auxiliary power and light. This was followed by the installation of similar equipment on the heavy cruiser *Quincy* and the aircraft carrier *Yorktown*. The success of these installations led to the standardization of alternating current for ship's service on all naval craft with the exception of submarines and certain auxiliary craft. All new vessels now under construction, including battleships, cruisers, aircraft carriers, destroyers, and large auxiliary vessels, have this type of ship's service power.

Experience in a-c applications has shown a large decrease in maintenance when compared with d-c machinery. A-c generators and motors eliminate commutators with all their attendant electrical and maintenance troubles. Also, alternating current permits the use of squirrel-cage induction motors which are probably the most rugged and reliable type of rotating electric machinery obtainable. All except the largest sizes of squirrel-cage motors may be started with simple across-the-line switches instead of the complicated controllers with contactors, coils, relays, resistances, and so on, required for d-c motors.

SELECTION OF VOLTAGE

The generators for the *Farragut* were designed for 230 volts. The use of transformers for voltage reduction on lighting circuits was avoided by bringing out taps from the midpoints on each phase of the generator and thus providing 115 volts. This scheme, however, is not entirely satisfactory since it requires special leads and also may result in unbalance in the generator winding. Present practice is to use a standard form of generator winding and provide transformers where voltage reduction is required.

As alternating current was applied to larger vessels it became apparent that a still higher voltage would be desirable from the standpoint of generator design and power distribution. Low-voltage generators of relatively large output require few turns and large conductors in the armature winding. This may be obtained by various methods, such as the use of few armature slots, single-layer armature windings, delta connections, parallel circuits, or other special arrangements which prevent using the most desirable generator design with regard to construction, maintenance, and weight.

For a given kilovolt-amperes, an increase in voltage results in a decrease in the current and this permits the use of smaller circuit breakers and distribution cables. On most equipment there is little if any change in insulation up to approximately 600 volts, so the current reduction is the more important factor.

A value of 450 volts was eventually adopted as the standard for ship's service generators. Power to practically all the auxiliaries is also distributed at 450 volts. For lights and for a few small accessories 115 volts is obtained from transformers. The use of 440-volt motors presented no particular problems since motors for this voltage were commercially available in all sizes down to one-sixth horsepower and it was only necessary to make certain minor changes in materials and enclosures to render the motors suitable for shipboard use.

GENERATORS

Up to the present time generators of normal design have been acceptable for ship's service duty. Turbine-driven generators with cylindrical rotors are required to have a short-circuit ratio of not less than 0.9 and salient-pole machines of not less than 1.0. No definite standards for the various reactances have as yet been adopted but a careful analysis is made for each system to insure stability and short-circuit protection. In the case of cylindrical-rotor generators the reactances are kept as high as possible consistent with a short-circuit ratio of not less than 0.9.

Generators for shipboard use are required to be of a type so designed and constructed as to insure satisfactory operation under the extreme conditions of heat, salt air, the rolling and pitching of the vessel in a heavy sea, and the shock of gunfire or mine explosions, all of which are incident to operation in a naval vessel. Some of the standard requirements follow.

1. *Short Circuit.* Generators must be capable of withstanding a dead short circuit, either three phase or single phase, for a period of two minutes without injury when operating at rated load and voltage.

2. *Overload Capacity.* Turbine-driven generators must operate at the rated power factor, under a current overload of 25 per cent, for a period of two hours, without objectionable noise or vibration and without exceeding the temperature rise specified for normal load by more than 15 degrees centigrade.

Turbine-driven generators must operate satisfactorily at 50 per cent kilowatt overload and approximate rated power factor for a period of five minutes.

Diesel-engine-driven generators must operate satisfactorily at 50 per cent kilowatt overload and approximate rated power factor for a period of five seconds.

3. *Phase Unbalance.* Generators must be designed to carry a maximum single phase load of 25 per cent of their rated line amperes. Under the condition of a single-phase load of 25 per cent of rated amperes and no other load on the machine the variation in voltages between phases or of any one phase from normal must not exceed 5 per cent of normal rated voltage.

Some of these standard requirements are more rigid than those of the usual industrial application; however, they have been adopted in view of the vital importance of these generators and in order to insure rugged equipment and continuity of operation.

PROTECTION OF OPERATORS

When 450 volts was adopted as a standard generator voltage it became necessary to make some changes in order to eliminate increased hazard to the operating personnel. In the older d-c systems the operators dealt with either 115 or 230 volts. It must be remembered that for a given nominal voltage the a-c peak voltage is 41 per cent higher than the steady value of d-c voltage. Furthermore, where the d-c values had formerly been 115 and 230 volts, the peak a-c voltage would now be 1.41×450, or 635 volts.

In order to afford adequate protection against this voltage, "dead front" switchboards came into use. On these boards it is impossible for the operator to come into contact with any live part of the circuit, since the circuit breakers, switches, bus bars, fuse clips, and other devices are all mounted on the back of the board and are suitably insulated. In addition, the back of the board is enclosed with a screen or perforated metal cover and a drip-proof cover is fitted on the top.

All motor and generator terminals are enclosed or insulated and control boxes have either mechanical or electric locks. Push buttons for starting and stopping operate on small transformers which reduce the voltage to 110 volts or less. Some of these protective features have complicated the switchboard to some extent but in general they have resulted in a much safer and more satisfactory system.

SHORT-CIRCUIT PROBLEMS

One of the problems associated with the adoption of alternating current for ship's service was that of short-circuit currents and the provision of suitable protective devices and operating relays. It is necessary not only that these accessories operate satisfactorily from the electrical standpoint but also that they possess shockproof characteristics, a feature not normally required in industrial applications. For example, a circuit breaker is equipped with a latch which mechanically holds the breaker closed and with a trip coil whose function is to trip this latch when a predetermined overload occurs. These two must be balanced to insure that the latch will not accidentally be opened by jar or shock and yet will open at the overload. Another example is that of relays, which respond to very small forces and hence must be carefully designed and adjusted. Yet we must be certain that their inclusion in the circuit is not such that they will cause the circuit to be interrupted under heavy shock. The application of this equipment for shipboard use thus opened a new field of engineering design and testing technique.

On larger vessels the ship's service generators are often of the turbine-driven two-pole cylindrical-rotor type, which normally has low values of reactance. In case of a short circuit with no external impedance in series such generators produce large short-circuit currents, sometimes reaching instantaneous subtransient values of 30 to 40 times normal. These currents are of only momentary nature but they create stresses in the generator windings, foundation bolts, bus bars, and various other parts of the equipment.

On one of the largest vessels now under construction, calculations show that the forces between adjacent switchboard bus bars under short circuit conditions may be as high as four tons. Such forces require that every bolt and brace be examined. Shearing stress on the bolts runs as high as 15,000 pounds per square inch and the stresses on insulating compounds and metallic supports are all of a high order.

It should be recognized that these stresses apply only to the largest vessels and have been brought about by the greatly increased use of electricity for auxiliary operation. The electrically propelled battleships previously described have a total d-c generating capacity of only 1,800 kw for supplying power to the auxiliaries, lighting system, and excitation for the propulsion generators. The two largest aircraft carriers *Lexington* and *Saratoga* have d-c generators totaling 4,500 kw for like purposes. Battleships now under construction have a-c installations of as much as 10,000 kw or more for the operation of the auxiliaries and lighting purposes. The distribution of this amount of d-c power on board a naval vessel presents difficulties which leave no doubt concerning the desirability of using alternating current. The individual generating units in these large installations are usually of 1,000 or 1,250 kw capacity, and high-speed cylindrical-rotor machines of this size have distinct advantages despite the fact that they have inherently low reactance. The increased stress problems are therefore a result of the increase in the size of the electrical plant and the employment of equipment best suited to these large installations.

Several outstanding types of bus construction have been employed that are capable of withstanding, with a large safety factor, the highest calculated stresses which might be set up. As the entire force is applied within the first half-cycle after the fault occurs, the bus is subjected to an impact which will result in distortion if the bus is not properly designed. All details of the structure supporting the buses have received careful attention to make them suitable for the forces involved. On the newest ships, switchboard structures are being designed of welded steel members, gusseted and braced in all directions. Attention has been paid to every portion of the structure from the smallest bolt to the heaviest channel. Adequate resistance has thus been provided for the short-circuit forces which may occur.

CIRCUIT BREAKERS

For these large a-c installations it became necessary to develop a circuit breaker suitable for the increased current carrying and interrupting capacity. It was felt that this circuit breaker should meet the following requirements:

1. Be mounted in small space on dead front boards.
2. Be dead front in itself and still accessible for maintenance and repair.
3. Provide interrupting rating equal to or better than that of a fuse without requiring replacement of a vital part of the element after an interruption.
4. Provide some cable protection without opening on the current inrush of a-c motors started directly across the line.

Such a breaker was available for commercial use in a

General Electric photo

A turbine-electric generating unit, to produce propulsion power on an electric-drive ship, being prepared for test somewhat different form and consisted of a totally enclosed unit with a thermal overload-tripping mechanism. This had certain disadvantages and failed to meet the second and fourth requirements just stated. It was felt that a thermally operated breaker was not strictly suitable for the machinery spaces of a naval vessel where the ambient temperature varies greatly, depending upon steam conditions, load being carried, ventilation, and changes of climate. Therefore, a form of breaker was developed having a high-set magnetic-trip unit which is instantaneous in action. The value at which this mechanism will operate is set high enough to permit the starting of motors without interruption of the circuit and still provide short-circuit protection for the circuit.

These breakers are now designated as type *AQB* (air, quenched break). The problem of making them accessible for repair or maintenance was quite difficult. On the earliest switchboards they were not readily accessible, because copper buses and cables at the rear of the board had to be removed to permit withdrawal of the breaker. However, the problem has been solved and at the present time these breakers are designed to be removed from the front of the switchboard without danger to personnel, without disturbing cable or bus connections, and without shutting down the rest of the system.

The application of these breakers has followed certain patterns depending upon the type of vessel, the type of switchboard, and the amount of power involved.

On destroyers where the amount of auxiliary power is fairly small the *AQB* breakers are connected directly to the bus and serve to control large blocks of power without making excessive space demands. The more conventional heavy-duty *ACB* (air, carbon break) type of breaker is used for controlling the primary generator circuit, and with its greater interrupting capacity serves to back up the smaller breaker, should it fail to clear the circuit under fault conditions.

On larger vessels a modified form of this back-up protection is used. Each group of *AQB* breakers is segregated and connected to the bus through a heavy-duty *ACB* breaker, which provides secondary protection without necessitating the removal of a generator from the bus.

A third pattern has been the exclusive use of heavy-duty type *ACB* breakers on the main switchboard. Each *ACB* breaker then feeds a remotely located power panel where the circuits are subdivided as necessary through *AQB*-type breakers. On such switchboards even these back-up breakers are removable from the front of the board without disturbing the bus work at the rear.

Rigid tests conducted on these circuit breakers show that it is usually 1 to $1^1/_2$ cycles from the instant the overload is applied until the arc is extinguished. This short time required for opening allows some decay in the subtransient current but still the breaker may be required to interrupt as much as 50 per cent of the initial peak value. At present no breaker of this type is used with an interrupting capacity of less than 15 times the normal rating.

OVERLOAD PROTECTION

Because of the relatively large currents drawn by squirrel-cage induction motors when started on full voltage, the problem of overload protection is different from that found on d-c motors. Above the fractional-horsepower sizes, starting resistances are required to limit the starting current on d-c motors. The resultant low starting current provides ample starting torque and also permits the use of fuses or magnetically operated circuit breakers which provide protection from overload as well as from short circuits.

The squirrel-cage induction motor, however, may have a full voltage starting current of 600 per cent or higher and although this current lasts for only a matter of seconds it precludes the use of fuses for overload protection, unless they are of special design or are short-circuited during the starting period. In any case fuse failure requires a replacement, and a device that may be reset automatically or manually provides a much better solution. Present practice therefore is to use some form of thermal overload relay.

The heating produced by an overload depends upon its magnitude as well as the length of time it is applied. Momentary overloads of short duration may not be injurious and in such cases circuit interruption is unnecessary and undesirable. In order to prevent instantaneous operation of the overload relay some form of time delay is usually incorporated in the device. This time delay may be accomplished by various methods but overload relays for naval service usually employ one of the following:

1. A bimetallic strip consisting of two metals having different temperature-expansion coefficients. The heat produced by overload causes unequal expansion of the metals and results in a deformation of the strip which trips the overload device.
2. A type in which an alloy is melted by a heater element and allows the circuit to open.
3. A type in which quick motion is retarded by an oil-filled dash pot.

A situation which further complicated the problem of overload relays was the extreme variation in ambient temperature occurring in certain locations on the ship. For a given setting a high ambient temperature might cause the overload relay to trip with rated load on the motor while at a low ambient temperature the relay might not operate until after the load exceeded a safe value. On first thought, this might appear to make no difference, but it must be remembered that the relay and the apparatus it controls may not always be so located that the ambient temperature is the same for both. Furthermore, the load that a machine can carry is not entirely dependent upon the maximum operating temperature; there are also mechanical limitations.

Since it is not practicable to adjust the relay setting manually to conform with different ambient temperatures, some form of compensation was required. This compensation is achieved by various methods and overload relays are now available which are so well compensated that they will operate at a definite current value with an ambient temperature variation of 20 to 70 degrees centigrade. When these overload relays are to be located in places of variable temperature they are specified to be of the compensated type. Navy specifications for this type of relay now allow three per cent change in the tripping current for each change of 10 degrees centigrade in ambient temperature and this results in reliable protection during any temperature changes ordinarily encountered.

OPERATING RELAYS

A-c holding coils, solenoids, contactors, and other devices require a number of mechanical and electrical features that are unnecessary in d-c equipment used for similar purposes. One problem is the reduction of the core loss produced by the flux reversals in the magnetic circuits. In order to avoid excessive core heating it is necessary to use low-loss steel and to laminate the core punchings. Magnetic noise or humming is suppressed by securely clamping the punchings together.

Another problem is the relatively low holding power of a-c magnetic contactors in the closed position. The flux linkages and reactance are low in the open position permitting sufficient current to flow to close the contactor. After the magnetic circuit is closed, however, the reactance is greatly increased and the exciting current of the coil is thus reduced. Furthermore, with each current reversal the flux passes through zero and this results in a tendency for the contactor to chatter. The usual remedy for this chattering is the use of a short-circuited shading coil on one of the poles of the magnetic contactor.

These undesirable features of a-c relays had been successfully overcome in industrial applications and they are mentioned here only because further refinements were necessary to make them suitable for naval use.

VOLTAGE REGULATORS

In changing over to alternating current it was apparent that some form of generator voltage regulator would be required. Low values of voltage regulation are not difficult to obtain in d-c machinery and all the older installations had operated in parallel without the use of regulators or equalizer connections. However, it should be recognized that some variation in ship's service voltage was not particularly objectionable at that time. In recent years there have been certain applications which require a comparatively constant voltage for accurate operation. An example is the synchro-tie control for gun firing. It may be said, then, that even if present installations were d-c, voltage regulators or some form of apparatus for maintaining constant voltage would likely be required.

The inherent regulation of a normally designed a-c generator operating at 80 per cent power factor may be as high as 40 per cent. This value can be lowered by altering the design of the generator but with results of uneconomical use of materials, increased field heating, and low values of reactance. Low reactances in turn allow large short-circuit currents which produce abnormal stresses in the end turns of the generator armature, require large interrupting switches, and are in general undesirable. In order to use generators of normal design the use of a voltage regulator thus was unavoidable. At the present time standards require that the regulation of the a-c generator must not exceed 38 per cent. The actual value of regulation, however, is relatively unimportant since the regulator is necessary with this type of generator.

In some respects the voltage-regulator requirements are more exacting than in the usual generating station. The large ratio of generating capacity to connected motor load that is usually found in central stations is not present on board ship; therefore, the starting of one large induction motor may have considerable effect on the voltage of the generator to which it is connected. For this reason it frequently becomes necessary to make large instantaneous changes in the field of the ship's service generator.

Some changes were required in the regulators commercially available to make them suitable for naval service. The principal requirements were that they be capable of effecting large instantaneous changes in the generator field current, that they resist the effects of salt-laden sea air and oily vapors of the engine room, and that they be sufficiently shockproof to permit satisfactory operation. These requirements have been met in the regulators now in naval service and the success of the present a-c ship's service systems is in no small measure due to the reliable operation of this relatively small but important piece of equipment.

INSTRUMENTS AND INSTRUMENT TRANSFORMERS

In addition to the voltmeters and ammeters required for a d-c system, the a-c system requires a power-factor meter and a wattmeter. Parallel operation also requires the use of a synchroscope.

The problem of switchboard design is further complicated by the necessity for instrument transformers. These fall into the two general classes—potential transformers and current transformers. The former are used to step down the 450-volt bus voltage to a nominal 115 volts for use on instruments, meters, and relays. The latter are used to step down the current from its large primary value to a nominal five amperes. This provides two advantages: first, it completely insulates all instrument terminals from the bus or generator circuit;

and, second, it performs the same function as a shunt in a d-c circuit and accurately portrays on an instrument the actual current in the bus without requiring that heavy current leads and impossibly large instruments be placed on a control switchboard. The first factor eliminates one additional operating hazard; the second is a practical necessity.

But while such transformers exist on all modern a-c switchboards their application to naval switchboards demands special technique. Careful circuit planning is required in order that the failure of an instrument transformer may not interrupt the operation of the system.

A second factor which influences the design is the fact that dangerous potentials exist on the secondary of a current transformer when the primary is excited and the secondary has accidentally been opened. This possibility likewise exists in commercial installations, but there the current transformers are not subject to gun fire and mine explosions, conditions which may cause accidental circuit interruption. This possibility has brought about the adoption of an automatic voltage-limiting device which acts to close the current transformer circuit when certain voltages are reached, thus removing any operating hazard.

POWER FACTOR

The problems introduced by the power factor of the a-c ship's service system were different in many respects from those of the usual industrial installation. The anchor and cruising loads could be expected to be reasonably steady, but the armament auxiliaries that are brought into action during battle conditions comprised loads of a highly fluctuating and intermittent nature and furthermore they require the largest motors installed for ship's service. Careful planning was therefore necessary to prevent excessive heating or voltage reduction on the various distribution circuits during periods of low power factor and peak loads.

The chief advantage of the a-c system was that it permitted the use of squirrel-cage induction motors in conjunction with full-voltage starting equipment. However, the use of this equipment imposed severe requirements on the generating and distribution systems. Unfortunately the starting current of an induction motor on full voltage may be five to six times normal current and the power factor during the starting period may be as low as 15 to 20 per cent. The time for reaching full speed will depend upon the starting torque of the motor as well as upon the apparatus being accelerated, although it is usually a matter of only a few seconds. Such operating conditions demand a quick-acting voltage regulator and a generator with considerable stability.

During the period of d-c operated auxiliaries, it was customary to install motors with greater capacity than was actually required. This practice resulted in excess weight but it insured ample overload capacity, low operating temperatures, and worked no particular hardship on the generator. The first induction motors installed had similar ratings and hence operated at less than full load. This created an undesirable operating condition since the power factor of an induction motor decreases as the load is reduced. The result was that the first a-c systems had a low power factor. The solution, of course, was to discontinue the practice of overmotoring. Practically all induction motors now used for ship's service are either two-, four-, or six-pole machines and have a reasonably good power factor at full load. Since all standard machines have at least 200 per cent maximum torque, sufficient momentary overload capacity is provided. In general the power factor of the usual ship's service system now varies from 85 to 95 per cent and thus permits the use of an 80-per-cent-power-factor generator.

POWER TRANSFORMERS

When generating ship's service power at 450 volts, transformers are necessary for lighting and for a few small miscellaneous applications. However, this low voltage load seldom exceeds 5 per cent of the total connected load and the weight of the transformers is more than compensated for by the weight reduction in other apparatus.

Provision for restricted emergency lighting with equipment capable of withstanding severe shock has long been a problem in naval service. The 6-volt automobile headlight type of bulb was best suited to this service but the extremely large and heavy feeders necessary for distribution made its use prohibitive. The usual practice on d-c systems was to supply this emergency lighting from storage batteries at approximately 30 volts. The batteries, however, with their excessive maintenance, frequent replacement, weight, and space requirements did not provide an entirely satisfactory solution. The adoption of alternating current permitted the use of small stepdown transformers used in conjunction with the more rugged 6-volt light bulbs. The transformer may be arranged to supply a group of lights or, where desirable, it may be compactly fitted into individual lighting fixtures. The flexibility of this arrangement provides a reliable emergency lighting system.

REDUCED-VOLTAGE MOTOR STARTING

Three-phase squirrel-cage induction motors of any commercial size may be designed for full-voltage starting. In addition, such features as normal starting torque with low starting current or high starting torque with low starting current may be provided. Whether or not the motor may be started on full voltage will depend upon the ability of the generator and voltage regulator to maintain a voltage sufficiently high to prevent the interruption of operation of other motors, lighting circuits, and control devices. The limitations of the usual ship's service generating plant require that the largest motors be started on reduced voltage and this is usually accomplished by the use of autotransformers with suitable starting taps.

A rule that has been used in naval design for determining whether a motor might be started on full voltage is that the locked rotor current of the motor must not exceed 50 per cent of the full load current of the generator supplying the power. This rule is not an absolute standard and in certain cases the 50 per cent value may be decreased or increased after an analysis of the particular application has been made. On a battleship, for instance, it may be found that in a certain motor group a motor of 100 horsepower can safely be started across the line while in another group it may be necessary to use reduced voltage starting for a 75-horsepower motor. The frequency of starting may also influence the method selected. In general, it will be found that less than 20 per cent of the installed motor horsepower requires reduced-voltage starting.

VARIABLE-SPEED MOTOR APPLICATION

There are a few auxiliaries on board ship which require some degree of speed variation. Ventilating fans constitute the principal application of this type. The quantity of ventilating air required in different parts of the ship varies considerably owing to the extreme outside temperatures encountered in northern and tropical waters as well as to variation in interior temperatures when at anchor or under way. Current practice is to use two-speed squirrel-cage induction motors for this application. The motors have two distinct primary windings and the control is arranged to energize the one desired. The control is somewhat more complicated than that for a single-speed motor but operation consists of merely pressing a "high," "low," or "stop" button. The ventilating-fan motors will usually not exceed 10 per cent of the total installed motor horsepower.

Other applications requiring speed variation are the fuel-oil and circulating-water pumps. These are usually driven by either two-speed or four-speed squirrel-cage induction motors and may constitute 1 per cent to $1^1/_2$ per cent of the installed horsepower.

Approximately 1 per cent of the installed horsepower is of the wound-rotor induction type for variable-speed operation. These are chiefly applied to winches, capstans, cranes, and similar equipment.

A number of motors are connected to their loads through variable-speed hydraulic transmissions. The steering gear, gun-turret training gear, and anchor windlasses are examples of this application. The characteristics of the hydraulic transmission are so well adapted to the severe speed and torque requirements of these auxiliaries that this type of drive would be used with either d-c or a-c motors.

D-C POWER

Direct current is still required for a few purposes, the principal ones being searchlight supply and excitation for the a-c generators. Where small amounts of d-c power are required, as on destroyers, it is possible to design a satisfactory oversize exciter directly connected to each generator. This supplies power for the excitation of the generator and affords a reliable source of direct current for other purposes. This arrangement was adopted on the first two destroyers equipped with alternating current for ship's service and is the method followed today for that class of vessel.

For larger vessels this method was considered less desirable; therefore the use of motor-generator sets was adopted as the standard arrangement. Normally, two sets are used, each located in separate machinery spaces. Some consideration was given to the use of separate steam-driven d-c generators, but the small size, simplicity, and general dependability of the motor-generator set were considered to outweigh the slight advantage which an entirely independent unit might offer.

EMERGENCY GENERATORS

The problem of supplying an effective emergency source of a-c power was more difficult to deal with. Stand-by power on a d-c system can be supplied by storage batteries. This is an excellent source of power and has a good record for dependability. The problem of supplying an equally dependable source for the a-c system offered the following possibilities:

1. The use of a battery-powered motor-generator set.
2. The use of a steam-driven a-c generator.
3. The use of an internal-combustion-engine-driven a-c generator.

Fortunately a reliable high-speed light-weight Diesel engine became available and that type of prime mover was adopted for the following reasons:

1. In event of a failure of steam supply, or when desired for port use, the emergency supply would be immediately available.
2. The Diesel engine is dependable, quick starting, and may be located in any convenient part of the ship.
3. The Diesel-engine set would be capable of supplying power continuously, whereas storage batteries have a limited capacity.

These emergency Diesel sets may be started either by batteries or by compressed air. The usual practice is to employ batteries for sets up to 100 kw and air for those of greater capacity. These sets are capable of being brought from standstill to normal full load within ten seconds.

Much thought and time have been spent on the problem of properly designing these emergency sets and the control circuits to govern their operation. It is necessary to place such generators on the line almost immediately after the failure of the main generators, yet it is desirable to avoid unnecessary and premature starting. The problem is further complicated by the fact that a-c motors under load cannot always be transferred instantaneously from one source to another, especially when the phase relations are not the same. It becomes necessary at times deliberately to slow down the action of such transfer to allow the magnetic flux present in the motor to decay before reapplying voltage. The delay in transfer is, however, a matter of only a few cycles and must be carefully controlled to provide, in so far as possible, an uninterrupted flow of power throughout the ship.

EDUCATIONAL PROGRAM

The change from direct to alternating current introduced the problem of instructing the naval operating personnel in the fundamentals, installation, and operation of a-c equipment. This was accomplished by issuing instruction courses for use by electrician's mates on board ship under the supervision of the engineering officers, by establishing schools on shore for selected petty officers, and by special inspection trips and courses at a number of the manufacturers' plants.

TESTS AND SPECIFICATIONS

It has been previously mentioned that the requirements for naval service are more rigid than those of the usual industrial installation. New types of equipment, such as transformers, voltage regulators, etc., which were introduced with the adoption of alternating current, required exhaustive tests to determine their fitness for naval service. Well-equipped laboratories are maintained for performing these tests. The testing program has been carried on in conjunction with the efforts of the various manufacturers, and some tests of a highly special nature have necessarily been conducted in their research laboratories. Various test methods have been adopted and are revised as found necessary.

The task of writing suitable specifications for this type of equipment was a huge undertaking and its progress obviously depended upon the tests just mentioned and their results. Much has been accomplished in this direction and there are now specifications for all equipment adopted for use. New specifications and revision of those in existence are, of course, constantly required, because of the introduction of new ideas and developments, but this is only to be expected during the normal course of progress.

Table I. Comparison of A-C and D-C Motors

A-C—Squirrel-cage, induction type, three-phase, 60 cycles, 440 volts, 1,750 rpm

D-C—Shunt-wound, constant speed, 1,750 rpm, 230 volts

Horsepower	Per Unit Values					
	Weight		Cost		Space Requirement	
	A-C	D-C	A-C	D-C	A-C	D-C
1	1.0	0.88	1.0	2.34	1.0	0.97
2	1.0	1.00	1.0	2.12	1.0	1.44
3	1.0	1.42	1.0	2.33	1.0	1.55
5	1.0	1.00	1.0	2.52	1.0	1.03
10	1.0	1.27	1.0	2.52	1.0	1.58
15	1.0	1.40	1.0	2.57	1.0	1.90
20	1.0	1.75	1.0	2.49	1.0	1.88
25	1.0	1.54	1.0	2.42	1.0	2.16
30	1.0	1.26	1.0	1.94	1.0	1.60
40	1.0	1.33	1.0	1.88	1.0	1.77
50	1.0	1.35	1.0	1.84	1.0	1.86

Table II. Comparison of A-C and D-C Motors

A-C—Squirrel-cage, induction type, three-phase, 60 cycles, 440 volts, 1,160 rpm

D-C—Shunt-wound, constant-speed, 1,150 rpm, 230 volts

Horsepower	Per Unit Values					
	Weight		Cost		Space Requirement	
	A-C	D-C	A-C	D-C	A-C	D-C
1	1.0	1.32	1.0	2.28	1.0	1.95
2	1.0	1.42	1.0	2.34	1.0	1.56
3	1.0	1.45	1.0	2.91	1.0	2.06
5	1.0	1.35	1.0	2.80	1.0	1.71
10	1.0	1.40	1.0	2.57	1.0	1.91
15	1.0	1.29	1.0	2.40	1.0	1.80
20	1.0	1.40	1.0	2.28	1.0	1.70
25	1.0	1.26	1.0	2.20	1.0	1.60
30	1.0	1.34	1.0	2.13	1.0	1.67
40	1.0	1.18	1.0	2.04	1.0	1.57
50	1.0	1.61	1.0	2.04	1.0	1.75

COMPARISON OF A-C AND D-C EQUIPMENT

Tables I, II, III, and IV show weight, cost, and space requirements of a-c and d-c motors, generators, and control. The values were obtained from a leading manufacturer and may be considered typical of present construction. The tabulated values in Tables I, II, and III are based on standard industrial equipment; however, any change in the relative values for the two types of apparatus due to special naval requirements is negligible.

In the interests of economy each manufacturer has established a number of different frame sizes for motors and enclosures for starters. Various motor ratings may be built on a given frame size; however, the change from one size to another does not necessarily occur at the same point or rating for all manufacturers. For this reason similar tables made up from different manufacturers' data will show some variation in weights and space requirements. The intention here is not to give exact values for each rating but to show in general the relation between the two types of equipment. In practically all instances the d-c equipment is heavier, costs more, and requires more space. The motor speeds of 1,750 and 1,150 rpm were chosen because the majority of a-c motors now used on board naval vessels fall within this speed range.

In Tables I and II the comparison is made on the basis of 440-volt a-c motors and 230-volt d-c motors. A-c motors of the sizes listed in these tables cost and weigh the same for either 220- or 440-volt operation. There is an increase in price for d-c motors built for voltages above 230 volts and the motor may weigh more. Therefore, if the distribution and switching advantages of the higher voltage were utilized in the d-c system, the motors would be at a still further disadvantage.

Table IV shows a comparison of d-c and a-c generators, motors, exciters, and control used in a Diesel-elec-

Table III. Comparison of Starters for A-C and D-C Motors

A-C—Starting duty only, full voltage, 440 volts
D-C—Starting duty only, full voltage, 230 volts

Horsepower	Weight A-C	Weight D-C	Cost A-C	Cost D-C	Space Requirement A-C	Space Requirement D-C
1	1.0	2.00	1.0	2.15	1.0	2.84
2	1.0	2.86	1.0	2.57	1.0	2.84
3	1.0	2.86	1.0	2.57	1.0	2.84
5	1.0	2.78	1.0	2.57	1.0	2.84
10	1.0	1.44	1.0	1.11	1.0	3.70
15	1.0	1.66	1.0	1.11	1.0	3.70
20	1.0	1.66	1.0	1.11	1.0	3.70
25	1.0	1.81	1.0	1.11	1.0	3.70
30	1.0	1.38	1.0	1.14	1.0	3.13
40	1.0	1.81	1.0	1.14	1.0	3.13
50	1.0	1.90	1.0	1.14	1.0	3.13

Table IV. Comparison of A-C and D-C Diesel-Electric Propulsion Equipment

Basis: 12,000 shaft-horsepower twin-screw vessel having propeller speed of 140 rpm. Dimensions, weights, and costs are per unit values

	A-C System	D-C System
Propulsion motors		
Number	2	2
Horsepower	6,000	6,000
Length	1.0	1.12
Diameter	1.0	1.0
Full-load efficiency	97	94.7
Weight, each	1.0	1.95
Propulsion generators		
Number	8	8
Kilowatts	1,150	1,180
Length	1.0	1.49
Diameter	1.0	1.23
Full-load efficiency	97.5	94
Weight, each	1.0	1.85
Excitation motor-generator sets		
Number	4	4
Length	1.0	0.79
Maximum diameter	1.0	0.65
Weight, each	1.0	0.31
Propulsion control		
Number	2	2
Length	1.0	0.57
Height	1.0	1.0
Depth	1.0	0.78
Weight, each	1.0	0.55
Approximate space requirements	1.2	1.0
Total weight	1.0	1.47
Approximate estimated total cost	1.0	1.5

Table V. Principal Ship's Service Equipment

Generator Equipment

Machines	Kilowatts
A-c	
Eight 1,250-kw 80-per-cent-power-factor 450-volt three-phase 60-cycle 3,600-rpm steam-turbine-driven generators	10,000
Two 250-kw 80-per-cent-power-factor 450-volt three-phase 60-cycle 1,200-rpm Diesel-driven generators for emergency use	500
Total (13,125 kva)	10,500
D-c	
Eight 10.4-kw 120-volt direct-connected exciters for the 1,250-kw turbogenerators	83.2
Two 5-kw 120-volt direct-connected exciters for the 250-kw Diesel-driven emergency generators	10.0
Two 125-kw 120-volt generators on motor-generator sets for operation of searchlights	250.0
Two 12-kw 40-volt generators on motor-generator sets for welding	24.0
One 5-kw 120-volt generator on motor-generator set for interior communication	5.0
Total	372.2

Motors

Application	Number of Motors	Total Installed Horsepower
Squirrel-cage induction motors		
16-inch gun turrets and ammunition handling	58	3,900
Water pumps	89	1,285
Ventilating fans	153	1,150
5-inch gun mounts and ammunition handling	82	1,115
Cranes, winches, hoists, etc.	13	1,030
Compressors	9	600
Steering gear	18	450
Oil pumps	14	250
Refrigerating equipment for air conditioning	19	115
Workshop tools	33	105
Motor-operated steam and water valves	35	75
Other miscellaneous applications	170	390
Synchronous motors		
Motor-generator sets for searchlights	2	400
Total	695	10,865

Heating and Lighting Equipment

Application	Kilowatt Capacity
Galley cooking equipment	365
Incandescent lights	310
Heaters	140
Sick bay and battle-dressing station	80
Total	895

tric propulsion system. From the standpoint of weight and cost, considerable advantage is shown in this particular application for the a-c equipment. It should be recognized, however, that in some instances, depending upon the size, type, and duty of the vessel, the d-c propulsion system may have advantages which are not shown here. The system adopted will depend upon the proper consideration and weighting of all the various factors involved.

SHIP'S SERVICE EQUIPMENT ON A MODERN BATTLESHIP

In order to show how extensively electricity is now used for the operation of a modern warship, the ship's service equipment on a battleship will be briefly described. The total connected load is approximately 9,000 kw. The anchor load has been calculated as 2,000 kw and the normal cruising load 2,500 to 3,000 kw. The battle load will be about 5,000 kw. These values are only approximations since the load is subject to wide fluctuations. A large percentage of the installed horsepower is for intermittent operation and the ratings are therefore only nominal. These motors may be called upon to deliver 180 to 200 per cent of their name-plate rating for short periods of time.

In discussing the electric equipment of a modern battleship it should be remembered that under the most favorable prewar conditions it took approximately four years to complete the vessel. Thus it may not always be possible to utilize developments which may have occurred during the period of construction. Changes are constantly being made in combatant vessels to increase their effectiveness and reliability and such changes are usually reflected in the ship's electrical installation. Before a vessel is actually put into operation, "present practice" with regard to certain equipment may be superseded by new and better arrangements on later vessels. In view of the constantly occurring changes it is therefore impossible rigidly to define current practice covering all apparatus.

The principal auxiliary or ship's service equipment is listed in Table V. The eight turbine-driven generators are arranged two each in four separate watertight machinery spaces. A generator and distribution switchboard is also provided in each space for each pair of generators. Switches and bus tie feeders form a loop to permit any of the eight generators to be operated in parallel, or, if desired, they may be divided into four independent generating plants. The installed generating capacity of 10,000 kw is twice the estimated battle load, thus providing ample stand-by capacity.

The generators are equipped with a closed system of ventilation, and water coolers remove the heat from the ventilating air. Protection is provided by signal alarms in the lubricating-oil system which operate when the pressure falls below a safe value and by resistance-type temperature detectors embedded in the armature winding. Heating elements are installed below each generator to prevent damage from condensation during idle periods.

The two Diesel-engine-driven emergency generators and switchboards are located in separate compartments, one forward and one aft of the main machinery spaces. Each of these generators operates independently of all other generators. The engines are automatically started when the system voltage falls below a predetermined value. Starting is accomplished by air and the sets are capable of being brought from standstill to full load and speed in less than ten seconds.

Emergency power supply from these two sets is to be furnished to the following auxiliary equipment:

Lighting
Five-inch gun mounts
Radio transmitters and receivers
Auxiliary circulating pumps
Auxiliary condensate pumps
Fuel-oil service pumps (port use)
Control buses
Interior communication and fire control systems
Motor-operated main condenser valves

A direct-connected exciter is provided for each a-c generator. The exciters do not operate in parallel but provide excitation to their respective generators only. They are of the open type and are self-ventilated with dripproof protection.

The largest d-c generators on the vessel are those for supplying the searchlights. These are driven by synchronous motors and are capable of operating in parallel.

On this vessel 96.6 per cent of the generating equipment is of the a-c type. This figure is representative of the extent to which modern combatant ship auxiliaries are operated by alternating current. Table VI shows the various types of motors used for driving the auxiliaries.

Table VI. Motors for Auxiliaries

Type of Motor	Per Cent of Total Installed Motor Horsepower
Three-phase single-speed squirrel-cage induction	85.0
Three-phase multispeed squirrel-cage induction	9.6
Three-phase synchronous	3.7
Three-phase wound-rotor single-phase etc.	1.7
	100.0

The single-speed squirrel-cage motor has been used wherever possible. All of the largest motors are of this type and operate at either 1,200, 1,800, or 3,600 rpm (synchronous speed).

The two-speed motors are used for driving ventilating fans and operate at either 900 and 1,200 rpm or 1,200 and 1,800 rpm. This necessitates two separate stator windings in each motor. As previously mentioned, this two-speed arrangement is considered necessary in order to provide the proper quantity of air under all operating conditions.

The only synchronous motors used are the two machines driving d-c generators for searchlight operation. They operate at leading power factor to provide some corrective kilovolt-amperes for the system.

There are a few applications of wound-rotor motors for high starting torque or variable-speed operation but these comprise only a small part of the installed horsepower. The single-phase motors are fractional-horsepower size and are usually operated from lighting circuits.

The galley cooking equipment consists of ranges, coffee-making equipment, and electrically operated utensils. In addition to the 365 kw of cooking equipment there are approximately 25 horsepower of motor-operated devices, such as dough mixers, potato peelers, dish-washing machines and others.

The sick-bay and battle-dressing station equipment consists almost entirely of stationary apparatus such as water heaters, sterilizers, X-ray equipment. Most of this equipment operates on 115 volts in order to utilize commercial products as far as possible.

The distribution system has been carefully planned. There are four main generator and distribution boards and each of these feeds smaller lighting and power distribution boards which are located at various load centers. Duplicate feeders, each located in cableways on opposite sides of the ship, supply power to the gun turrets, antiaircraft guns, central stations, damage control pumps, battle lighting, etc. The steering-gear motors have a number of duplicate supply feeders. No cables are run through magazines and all cables for battle power and lighting are run below the protective deck and behind armor wherever possible.

NEW APPLICATION OF A-C MACHINERY

The Navy now has under construction two submarine tenders which are very similar except that one will be equipped with a Diesel-driven d-c propulsion system and the other will have Diesel-driven a-c propulsion. Both vessels will have a-c ship's service power. The Diesel a-c propulsion system will constitute the first such application on a naval vessel. Likewise this will be the first use of synchronous motors for propeller drive in the Navy. A general description of this equipment follows:

Two separate watertight machinery spaces are provided for the propulsion equipment. Four Diesel-engine generator sets, one propulsion motor, an excitation motor-generator set, and one control unit are located in each compartment. The two propulsion systems operate independently of each other.

The generators are rated 1,150 kw, 95 per cent power factor, 2,400 volts, 759 rpm, three-phase, 62.5 cycles. They have a closed self-ventilating system with surface-type water coolers. Two generators in each compartment are used for propulsion power only and are excited from a separate motor-generator set. The other two generators in the same compartment have direct-connected exciters and may be used either for propulsion or for parallel operation with the regular ship's service generators during periods of excessive power demands for submarine servicing. Amortisseur windings are provided in all generators to minimize hunting action.

The propulsion motors are of the synchronous type and are rated 5,900 horsepower, 2,400 volts, three-phase, 62.5 cycles, 139 rpm. Each motor is directly connected to its respective propeller shaft. The ventilating system is closed and equipped with water coolers. Two separate motor-driven fans supply forced ventilation for each unit. Amortisseur windings are provided for starting, maneuvering, and for limited operation at speeds less than 25 per cent of normal. Dynamic braking is employed to aid in stopping the propeller prior to reversal.

The motor-generator set supplying excitation for this synchronous machinery has two separate generators. One is sufficiently large to furnish excitation for four propulsion generators and the other supplies excitation for the propulsion motor. The fields of these two exciters are regulated by a single pilot exciter. Normally, each exciter will supply its respective propulsion motor or propulsion generators. However, during the starting, reversing, maneuvering, and slow-speed periods of the propulsion motor, when overexcitation is required on the propulsion generators, both exciters may be connected in series across the propulsion generator field to provide the necessary field forcing.

Propeller-speed variation between 100 per cent and 25 per cent of normal is obtained by regulation of the fuel supply to the Diesel engines. When operating in parallel, the adjustment is made simultaneously on all engines. The frequency of the synchronous machinery thus varies between 16 and 62.5 cycles for the foregoing speed variation. Provision is made for simultaneous and automatic rheostatic adjustment of the propulsion generator and motor fields at various speeds and loads.

Where power transformers are required, naval practice in the past has been to use the air-cooled single-phase type. These units are delta-connected and, in case of failure, one unit may be disconnected and the remaining two operated open delta. Although the weights are increased by using air-cooled rather than oil-cooled types, the use of oil has been prohibited because of the fire hazard.

On this submarine tender, two fairly large transformers are used in conjunction with the propulsion generators, which are arranged to supply additional power for ship's service. In order to reduce weight, three-phase units are being used. They are rated 1,000 kva, 2,400/450 volts. The low-voltage winding is delta-connected. Additional weight saving is effected by the use of a noninflammable insulating and cooling medium in the transformer. These noninflammable liquid-filled transformers are now widely used in industrial installations. Their application here illustrates how industrially developed apparatus may often be used to advantage by the Navy.

On a vessel of this type, with the great variety of shop equipment, cranes, foundry, etc., the ship's service connected load will necessarily be quite high. In this case, loads of 2,500 kw are expected at certain times and the total connected load will be approximately 6,000 kw.

One rather severe requirement imposed on the generating system is the operation of two 125-kw single-phase arc furnaces installed in the foundry. To avoid this undesirable single-phase load, the use of three-phase units was considered. However, such units were not commercially available and the scheme finally adopted was to supply either one or both furnaces from a single 500-kw generator. In case only one unit is in operation, the single-phase load will not exceed 25 per cent of the generator rating, which value is within the allowable single-phase load. For operating two furnaces, a two-phase–three-phase transformer connection is used, which results in very little unbalance on the generator beyond that normally present in arc-furnace operation.

Another large auxiliary load is that of an 850-kw motor-generator set used for submarine battery charging. This set is driven by a synchronous motor and power is supplied from the propulsion generators.

The operation of this a-c propulsion system will be watched with great interest, since it provides an unusual opportunity for comparing d-c and a-c systems.

CONCLUSION

The advantages of alternating current for the large propulsion systems now in operation in the United

States Navy were obvious from the beginning. D-c generators and motors are limited to relatively low voltages, and for large power installations equipment suitable for handling the abnormal current was neither available nor practicable for shipboard installation. The a-c system was therefore adopted for all large propulsion systems.

The situation with regard to ship's service power presented a different aspect. D-c systems were in operation on existing vessels and the adoption of alternating current for new vessels involved a number of problems which have been treated in some detail. The change-over was made in the interests of the advantages previously outlined. It is believed that the operating record of these a-c systems for the past eight years and the present general approval by naval personnel offer sufficient justification for such a major change.

In conclusion, it is desired to acknowledge the valuable assistance contributed by the electrical industry in adapting a-c equipment to naval service. The representatives of the industry have willingly given their time and the benefit of their engineering experience and the success of these a-c systems is largely due to this generous co-operation.

Traveling Waves on Transmission Lines

ERNST WEBER
FELLOW AIEE

THE USE of the Laplacian transformation in combination with tables of Fourier integrals reduces the solution of transmission-line problems to the same status as evaluating definite integrals by means of tables of integrals. Though this extension of operational methods is as yet unfamiliar to many, its great power, if once grasped, will rapidly increase its use.

Although the analysis of transient phenomena on transmission lines can be carried out by the direct operational method,[1] it is greatly facilitated by the use of the Laplacian transformation,[3] the recent and probably most powerful extension of operational methods. This is true for any type of problem, be it in the field of electric-power transmission, or in the propagation of communication signals and speech, or in the transmission of broadcast sound and pictures. Like no other method, the Laplacian transformation permits a clear and consistent physical interpretation of each successive step in the analysis, which is extremely helpful when difficult decisions have to be made with respect to tolerable distortion, needed selectivity, required suppression of reflections, and other similar considerations.

THE LAPLACIAN TRANSFORMATION

Since various authors have used different notations and slightly different definitions, it seems necessary to give a brief exposition of the mathematical background so far as it is needed for a clear understanding of what follows.

There are perhaps no other physical phenomena in which the power and relative simplicity of the operational method are so apparent as in the solutions for traveling waves on transmission lines. This article develops the general expressions for the voltage and current at any point on the line. The traveling voltage waves of special interest to the power engineer, and the undistorted current wave of particular interest to the communications engineer, are discussed. Reflection factors are introduced and an example presented to illustrate their use.

This article is the fifth and last of a series presented as lectures before the basic science group of the AIEE New York Section in a symposium on "Advanced Methods of Mathematical Analysis as Applied to Electrical Engineering." The first article, on Heaviside's operational calculus, appeared in the February issue of Electrical Engineering and has since been followed by articles on integration in the complex plane, Laplacian transforms, and Fourier integrals. For a more complete and rigorous discussion of any of these topics the reader may refer to the list of references accompanying each article.

A consolidated pamphlet reprint of this series will be available shortly at a cost of $1 per copy; see page 321 for details.

PAUL C. CROMWELL, Chairman Symposium Committee
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If $f(t)$ is a physically possible but otherwise arbitrary function of time, then it usually permits the evaluation of the "Laplace integral" $\int_0^\infty \epsilon^{-pt} f(t)dt$, where p is an arbitrary complex parameter with $Re(p) > \alpha$, and α is the smallest value for which the integral converges absolutely, that is, for which

$$\lim_{t\to\infty}\int_0^t \left|\epsilon^{-\alpha t} f(t)\right| dt < \infty$$

The value of the Laplace integral, if it exists, is naturally a function of only the complex variable p, say $F(p)$. If one then writes

$$F(p) = \int_0^\infty \epsilon^{-pt} f(t)dt \quad Re(p) > \alpha \tag{1}$$

one has a unique relationship between a given function of the real time variable t and a new function $F(p)$ of the complex variable p. As a process, equation 1 is called the Laplacian transformation; as a result, $F(p)$ is called the Laplacian transform of $f(t)$, in symbols sometimes

$$F(p) = L\,f(t) \tag{1a}$$

The most important aspect of this transformation for all applications is the fact that $f(t)$ can be arbitrarily discontinuous, yet the transform $F(p)$ will always be a continuous function in p. Thus, as the simplest and most frequently used illustration, take "unit step" voltage

$$e(t) = \left\{\begin{matrix} 0 \text{ for } t<0 \\ 1 \text{ for } t>0 \end{matrix}\right\} = H(t) = \mathbf{1} = S_{-1}(t) \tag{2}$$

where, in succession, the terms indicate the mathematical definition, the notation as the "Heaviside function," and the Heaviside symbol, all having identical meanings. The Laplacian transform of this unit step is

$$E(p) = \int_0^\infty \epsilon^{-pt} dt = 1/p \tag{3}$$

an analytic function of p, regular in the entire p-plane with the exception of $p=0$, where it has a pole of the first order.

Again taking a "unit step" voltage, but now assuming that the discontinuity, or sudden rise, occurs at $t=T$, one has the definition

$$e(t) = \left\{\begin{matrix} 0 \text{ for } t<T \\ 1 \text{ for } t>T \end{matrix}\right\} = H(t-T) = \mathbf{1}_{(t-T)} = S_{-1}(t-T) \tag{4}$$

and the Laplacian transform becomes

$$E(p) = \int_T^\infty \epsilon^{-pt} dt = \frac{\epsilon^{-pT}}{p} \tag{5}$$

The shift of the discontinuity in the t-variable therefore is transformed into an exponential factor ϵ^{-pT} as the comparison with equation 3 shows. This relation is of utmost importance in connection with propagation phenomena.

Equation 1 defines the transformation from the domain of time functions into the domain of the complex p-functions. The inversion of this transformation is obtained by the complex line integral

$$f(t) = \frac{1}{2\pi j}\int_{c-j\infty}^{c+j\infty} \epsilon^{pt} F(p)dp \quad c > \alpha \tag{6}$$

which leads, however, to $f(t)=0$ for $t<0$. In the direct transformation given by equation 1, no specification of the function $f(t)$ for $t<0$ was required; it is now necessary to stipulate $f(t)=0$ for $t<0$ in order to make equations 1 and 6 consistent, to round out the process as a reversible one, and thus give it its great significance in applications to physical problems. The reasons cannot be elucidated here; for further information see list of references. The relation 6, as a process, is called the inverse Laplacian transformation and the result $f(t)$ is called the inverse Laplacian transform of $F(p)$, in symbols sometimes

$$f(t) = L^{-1}F(p) \tag{6a}$$

Applying the integration 6 to the transform 3 one again obtains the definition 2 with the additional statement that "unit step" voltage has the value $1/2$ at $t=0$, compatible with the Fourier integral concept of finite discontinuities.

TABLES OF FOURIER INTEGRALS AND THE LAPLACIAN TRANSFORMATION

The rigorous mathematical treatment of the Laplacian transformation rests heavily upon the Fourier integral theorems and indeed can be considered as a special case of these of particular practical applicability. Since the dual forms of the Fourier integrals can be written with $p = j\omega$ as

$$F(j\omega) = \int_{-\infty}^{+\infty} \epsilon^{-j\omega t} f(t)dt \tag{7}$$

and

$$f(t) = \frac{1}{2\pi}\int_{-\infty}^{+\infty} \epsilon^{j\omega t} F(j\omega)d\omega \tag{8}$$

one observes that equation 7 takes on the same form as equation 1 if $f(t)=0$ for $t<0$. Thus the tables of Fourier integrals can be used immediately as tables of Laplacian transforms, if one admits only cases with $f(t)=0$ for $t<0$.

The most complete collection of Fourier integrals is the one by G. A. Campbell and R. M. Foster;[5] in subsequent references to these tables the abbreviation "CF-tables" is used. Generally, this article employs a slightly different notation than given in the CF-tables, a notation which follows directly from a comparison of forms 7 and 8 with the basic forms on top of page 37 of the CF-tables.

LAPLACIAN TRANSFORMATION OF TRANSMISSION-LINE EQUATIONS

Accepting, for the purposes of this treatment, the simple conventional partial differential equations which describe in first approximation the current-voltage relationship for long lines,[6,7] we have

$$-\frac{\partial e}{\partial x}=+l\frac{\partial i}{\partial t}+ri \tag{9a}$$

$$-\frac{\partial i}{\partial x}=+c\frac{\partial e}{\partial t}+ge \tag{9b}$$

Here $e=e(x, t)$ and $i=i(x, t)$ are voltage and current, as functions of both distance x counted from the "sending end" $x=0$, and time counted from the instant of "disturbing the equilibrium" on the line. The inductance, resistance, capacitance, and leakance, of the line per unit length are indicated respectively by l, r, c, g. Equations 9 are frequently referred to as "the complete telegrapher's equations."

Though both voltage and current are functions of two variables, the concept of the Laplacian transformation can be applied with respect to the time variable t, considering x hereby as a continuously variable parameter. Multiplying both sides of both equations 9, term for term, by ϵ^{-pt} (where $Re(p)>\alpha$), and integrating with respect to t from 0 to ∞, one obtains the Laplacian transforms

$$\int_0^\infty e(x, t)\epsilon^{-pt}dt=E(x, p) \tag{10}$$

$$\int_0^\infty \frac{\partial e}{\partial x}\epsilon^{-pt}dt=\frac{\partial}{\partial x}\int_0^\infty e(x, t)\epsilon^{-pt}dt=\frac{\partial}{\partial x}E(x, p) \tag{10a}$$

$$\int_0^\infty \frac{\partial e}{\partial t}\epsilon^{-pt}dt=\left[\epsilon^{-pt}e(x, t)\right]_{t=0}^{\infty}+p\int_0^\infty e(x, t)\epsilon^{-pt}dt=-e_0(x)+pE(x, p) \tag{10b}$$

where $e_0(x)$ denotes the known initial voltage distribution along the line at $t=0$. In deriving equation 10*b* it was also assumed that $[\epsilon^{-pt}e(x, t)]$ vanishes at $t=\infty$, which is certainly true for all physically realizable cases, provided that $\alpha>0$. Exactly similar relations hold for the current $i(x, t)$ and its Laplacian transform $I(x, p)$.

The partial differential equations 9 become now in the complex p-domain

$$-\frac{\partial E(x, p)}{\partial x}=-li_0(x)+lpI(x, p)+rI(x, p) \tag{11a}$$

$$-\frac{\partial I(x, p)}{\partial x}=-ce_0(x)+cpE(x, p)+gE(x, p) \tag{11b}$$

Since p is to be considered a continuously variable complex parameter, x is the only remaining independent variable and equations 11 can be rewritten as the ordinary inhomogeneous differential equations

$$-\frac{dE}{dx}=-li_0(x)+(lp+r)I \tag{12a}$$

$$-\frac{dI}{dx}=-ce_0(x)+(cp+g)E \tag{12b}$$

where, again, E and I are the Laplacian transforms of voltage and current as defined by equation 10, and $e_0(x)$ and $i_0(x)$ denote the known initial distributions of current and voltage along the line. Equations 12, together with two given independent boundary conditions specifying current or voltage values, or a combination of both at sending or receiving ends of the line, completely state the most general transmission-line problem. The initial conditions are contained in the differential equations in the same manner as in lumped circuit problems,[8] so that the number of arbitrary integration constants is less than in the classical method.

GENERAL SOLUTION OF TRANSMISSION-LINE EQUATIONS

In order to effect a solution, equations 12 can be combined into a single differential equation of the second order in x by differentiating equation 12*a* with respect to x and substituting dI/dx in equation 12*b*, thus giving

$$\frac{d^2E}{dx^2}=l\frac{di_0(x)}{dx}-c(lp+r)e_0(x)+\gamma^2E=N(x)+\gamma^2E \tag{13}$$

where

$$\left.\begin{aligned} N(x)&=l\frac{di_0(x)}{dx}-c(lp+r)e_0(x)\\ \gamma^2&=(lp+r)(cp+g)=\frac{1}{v^2}[(p+\rho)^2-\sigma^2]\end{aligned}\right\} \tag{14}$$

with

$$v=\frac{1}{\sqrt{lc}},\quad \rho=\frac{1}{2}\left(\frac{r}{l}+\frac{g}{c}\right),\quad \sigma=\frac{1}{2}\left(\frac{r}{l}-\frac{g}{c}\right) \tag{15}$$

The general solution of equation 13 is the sum of the solution of the homogeneous differential equation

$$\frac{d^2E}{dx^2}=\gamma^2E \tag{16}$$

which is of the exponential type, and the particular integral of the inhomogeneous equation 13. Thus, one has for the Laplacian transform of the voltage

$$E(x, p)=A\epsilon^{\gamma x}+B\epsilon^{-\gamma x}+\frac{\epsilon^{\gamma x}}{2\gamma}\int\epsilon^{-\gamma x}N(x)dx-\frac{\epsilon^{-\gamma x}}{2\gamma}\int\epsilon^{\gamma x}N(x)dx. \tag{17}$$

where A and B are arbitrary integration constants. For the Laplacian transform of the current one obtains, by using equations 12*a* and 17

$$I(x, p)=\frac{1}{Z_c}\left\{-A\epsilon^{\gamma x}+B\epsilon^{-\gamma x}-\frac{\epsilon^{\gamma x}}{2\gamma}\int\epsilon^{-\gamma x}N(x)dx-\frac{\epsilon^{-\gamma x}}{2\gamma}\int\epsilon^{\gamma x}N(x)dx+\frac{1}{\gamma}li_0(x)\right\} \tag{18}$$

where

$$Z_c=\frac{lp+r}{\gamma}=\sqrt{\frac{lp+r}{cp+g}}=\sqrt{\frac{l}{c}}\cdot\sqrt{\frac{p+(\rho+\sigma)}{p+(\rho-\sigma)}} \tag{19}$$

is termed the characteristic parametric impedance of the

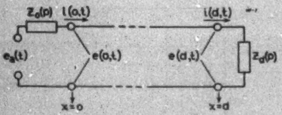


Figure 1. Schematic diagram of transmission line with general impedance terminations

line, since it contains only the characteristic line constants.

The integration constants must now be evaluated before the inverse transformation is applied, because in general they are functions of the parameter p. This means that the boundary conditions themselves must be expressed in terms of Laplacian transforms. Assuming the general terminations as shown in Figure 1 with $e_a(t)$ the applied voltage at the sending end, the conditions are in terms of the Laplacian transforms of all voltage drops at $x=0$:

$$E(0, p) = Le_a(t) - I(0, p)Z_0(p) \tag{20}$$

at $x=d$:

$$E(d, p) = I(d, p)Z_d(p) \tag{21}$$

where $Z_0(p)$ and $Z_d(p)$ are the conventional parametric expressions for the terminal impedances which for $p=j\omega$ become identical with the complex *a.-c.* impedance expressions.

Introducing into equations 20 and 21 the special values of $E(x, p)$ and $I(x, p)$ from equations 17 and 18 we arrive at two algebraic equations for A and B, so that these constants can be directly evaluated. It is then necessary merely to apply the inverse Laplacian transformation to the forms 17 and 18 with A and B replaced by their functional expressions. Usually, tables of Fourier integrals can be consulted, as is illustrated by a few examples. In special cases it might be convenient for the evaluation of the integrals involving $N(x)$ to use an alternative expression for $\frac{di_0(x)}{dx}$ which can be obtained from equation 9*b* by putting $t=0$ there, namely

$$\frac{di_0(x)}{dx} = \left[\frac{\partial i}{\partial x}\right]_{t=0} = -c\left[\frac{\partial e}{\partial t}\right]_{t=0} - ge_0(x) \tag{22}$$

SOLUTIONS FOR THE SEMI-INFINITE TRANSMISSION LINE

As a simple illustration, assume an initially de-energized $[N(x)=0]$ line extending to $x=\infty$, and with voltage $e_a(t)$ directly applied at $t=0$ to its sending-end terminals, $Z_0(p)=0$ at $x=0$. Since $Re(\gamma)>0$, $A=0$ must be assumed to assure finite values of current and voltage at infinite distance from the supply. This takes the place of the general boundary condition 21; moreover condition 20 reduces to

$$E(0, p) = Le_a(t)$$

Thus the total solutions for the Laplacian transforms 17 and 18 reduce in this simple case to

$$E(x, p) = \epsilon^{-\gamma x} \cdot Le_a(t) \tag{23}$$

$$I(x, p) = \frac{\epsilon^{-\gamma x}}{Z_c} \cdot Le_a(t) \tag{24}$$

with γ and Z_c defined by equations 14 and 19, and with the auxiliary definitions 15.

In order to apply the inverse Laplacian transformation one has to specify the applied voltage. Assuming it as a suddenly applied d-c voltage of value V, then

$$Le_a(t) = LVS_{-1}(t) = \frac{V}{p} \tag{25}$$

if one uses the notations $S_{-1}(t)$ of the CF-tables, pair 415, for unit step occurring at $t=0$. It is also implied that one chooses in pair 415 the value of the arbitrary constant $\lambda = +1/2$ so as to obtain $e_a(t)=0$ for $t<0$. With the definition 14 one now has to invert the Laplacian transforms

$$E(x, p) = \frac{V}{p} \cdot \epsilon^{-\gamma x} = \frac{V}{p} \cdot \exp\left[-\frac{x}{v}\sqrt{(p+\rho)^2-\sigma^2}\right] \tag{26}$$

$$I(x, p) = \frac{V}{p} \cdot \frac{\epsilon^{-\gamma x}}{Z_c} = V\left(c+\frac{g}{p}\right) \cdot \frac{\exp\left[-\frac{x}{v}\sqrt{(p+\rho)^2-\sigma^2}\right]}{\frac{1}{v}\sqrt{(p+\rho)^2-\sigma^2}} \tag{27}$$

where for the current transforms use has been made of equation 19 in an obvious transformation. Neither the current nor the voltage transform can be found directly in the CF-tables. However, pair 860.0 gives with a slight change in notation so as to avoid confusion of symbols:

$$L^{-1}\frac{\exp\left[-y\sqrt{(p+r)(p+s)}\right]}{\sqrt{(p+r)(p+s)}} = \epsilon^{-1/2(r+s)t} I_0\left[\frac{1}{2}(r-s)\sqrt{t^2-y^2}\right] \qquad t>y$$

where I_0 is the modified Bessel function of the first kind and zero order. Comparison with equation 27 shows the correspondence of symbols

$$y=\frac{x}{v} \qquad r=\rho+\sigma \qquad s=\rho-\sigma$$

so that the fraction of equation 27 has the inverse Laplacian transform

$$v\epsilon^{-\rho t} I_0\left[\sigma\sqrt{t^2-\left(\frac{x}{v}\right)^2}\right] \qquad t>\frac{x}{v} \tag{28}$$

To obtain the complete expression for the current, one can now use pair 210 of the CF-tables

$$L^{-1}\frac{1}{p}F(p) = \int_0^t f(t)dt \qquad \text{if } L^{-1}F(p) = f(t) \tag{29}$$

where the lower limit of the integral must be 0 rather than $(-\infty)$ because in the Laplacian transformation $f(t)=0$ for $t<0$. One finally has

$$\left.\begin{aligned} i(x, t) = \frac{V}{\sqrt{l/c}}\left(1+g/c\int_{x/v}^{t} dt\right) \cdot \left[\epsilon^{-\rho t} I_0\left(\sigma\sqrt{t^2-\left(\frac{x}{v}\right)^2}\right)\right] \\ t>\frac{x}{v} \end{aligned}\right\} \tag{30}$$

where the integration has to be performed on the bracketed term; the lower limit necessarily becomes x/v since the integrand vanishes for $t<x/v$.

This solution can best be interpreted if one assumes $g=0$, a nonleaky line, in which case from equation 15

$$\rho=\sigma=\frac{r}{2l}=\delta$$

and

$$i(x,\ t)=\frac{V}{\sqrt{l/c}}\epsilon^{-\delta t}I_0\left(\delta\sqrt{t^2-\left(\frac{x}{v}\right)^2}\right) \qquad t>\frac{x}{v} \qquad (31)$$

According to the definition of inverse Laplacian transforms, the current is zero for $t<x/v$, that is, for every point x along the line it takes a time x/v before a current can be measured even with the most sensitive ideal instruments. In other words, the current, flowing as a result of the applied voltage $VS_{-1}(t)$, propagates along the line with a velocity $v=\frac{1}{\sqrt{lc}}$ (see equation 15) and arrives at a point x with magnitude

$$i_{(t=x/v)}=\frac{V}{\sqrt{l/c}}\cdot\epsilon^{-\delta x/v}=\frac{V}{R_s}\cdot\epsilon^{-\delta x/v} \qquad (32)$$

Since $I_0(0)=1$, it appears therefore as if the initial current, of value $\frac{V}{\sqrt{l/c}}$ at the sending end, is attenuated along the line with the attenuation constant $\delta=\frac{r}{2l}$. At the sending end, the application of the voltage V produces an initial current $\frac{V}{\sqrt{l/c}}$; this is the same result as if the line for the first instant were considered as a resistance of value $R_s=\sqrt{l/c}$ which is called the "surge resistance" of the line. As time increases and $t>\frac{x}{v}$ a current variation is observed at the point x which is essentially given by the second bracket in the form.

$$i(x,\ t)=\left[\frac{V}{R_s}\cdot\epsilon^{-\delta\frac{x}{v}}\right]\left[\epsilon^{-(\tau-\tau_0)}I_0(\sqrt{\tau^2-\tau_0^2})\right] \qquad \tau>\tau_0 \qquad (33)$$

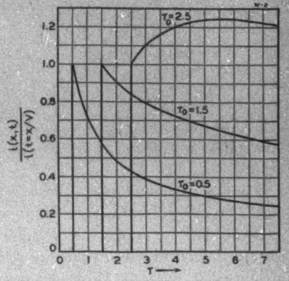
where

$$\tau=\delta t, \qquad \tau_0=\delta\cdot\frac{x}{v}$$

Here, τ can be called "numerical time" and τ_0 is a measure of the distance x from the sending end. Figure 2 shows the time variation of the current at three different distances; obviously there is a great discrepancy between the applied unit step voltage and the observed current variation so that from the point of view of signal transmission such a nonleaky line is not desirable.

In power transmission it is imperative to insulate a line exceedingly well since the objective is to transmit power and not to lose it on its way. Usually, the voltage is of primary interest because its "wave front," that is, the time-space variation at the head of the voltage wave, determines the dielectric stresses in the insulating materials of terminal equipment. For the general line, the inverse Laplacian transform of equation 26 can be found by pair 863.1 of the CF-tables which gives with the same notation as used above in the case of the current:

Figure 2. Current response on nonleaky line at various points along the line in case of unit step voltage applied at sending end



$$L^{-1}\exp\left[-y\sqrt{(p+r)(p+s)}\right]=\frac{y(r-s)}{2\sqrt{t^2-y^2}}\epsilon^{-1/2(r+s)t}\times$$
$$I_1\left[\frac{1}{2}(r-s)\sqrt{t^2-y^2}\right]+L^{-1}\epsilon^{-yp}\exp\left[-\frac{1}{2}y(r+s)\right] \qquad t>y \qquad (34)$$

where now I_1 is the modified Bessel function of the first kind and first order. The second term on the right-hand side is the delayed unit impulse of pair 601 with magnitude $\epsilon^{-\rho x/v}$ in our notation. Again using equation 29 in order to account for the additional factor $1/p$, one finally has

$$e(x,\ t)=\sigma\frac{x}{v}V\int_{x/v}^{t}\frac{\exp(-\rho t)}{\sqrt{t^2-\left(\frac{x}{v}\right)^2}}I_1\left(\sigma\sqrt{t^2-\left(\frac{x}{v}\right)^2}\right)dt+$$
$$\epsilon^{-\rho\frac{x}{v}}VS_{-1}\left(t-\frac{x}{v}\right) \qquad t>\frac{x}{v} \qquad (35)$$

This form is much less clear even if one assumes now $g=0$ because of the integral involving the Bessel function. However, a series expansion in terms of Bessel functions of increasing order has been derived[8] which is considerably more convenient for numerical computations.

CONCEPTION OF THE DISTORTIONLESS LINE

Since the general case and even the nonleaky line lead to rather involved expressions for the current and voltage distributions, and indicate strong attenuation and distortion of signals impressed at the sending end, it might be well to examine the characteristic quantities again for special cases. In the defining equations 14 and 15 only σ can be made to vanish. Assuming, therefore

$$\sigma=0 \qquad \frac{r}{l}=\frac{g}{c}=\rho \qquad \gamma=\frac{p+\rho}{v} \qquad (36)$$

one can reduce the current solution 30 to

$$i(x,\ t)=\frac{V}{R_s}\cdot\epsilon^{-\rho\frac{x}{v}} \qquad t>\frac{x}{v}$$

by performing the indicated integration and observing that $I_0(0)=1$. On account of $i(x, t)=0$ for $t<\frac{x}{v}$, one can also write

$$i(x, t)=\frac{V}{R_s}\cdot\epsilon^{-p\frac{x}{v}}\cdot S_{-1}\left(t-\frac{x}{v}\right) \tag{37}$$

From equation 35 follows then

$$e(x, t)=V\cdot\epsilon^{-p\frac{x}{v}}\cdot S_{-1}\left(t-\frac{x}{v}\right) \tag{38}$$

and one at once deduces that a transmission line with parameters defined by equation 36 attenuates the unit step exponentially along the line but does not distort it. This can be generalized for any type of applied signal if equation 36 is introduced into equations 23 and 24. Consequently, such a transmission line is called "distortionless" and is the aim of all communication design for sound and picture transmission. In general, equation 36 demands a very high value of leakance g if one accepts the conventional values of r and l. The better procedure, therefore, is to increase the inductance l either by insertion of loading coils[9,10,11] at periodic intervals or by continuously loading the line in form of a thin steel tape[12,13] wrapped upon the copper core. The first procedure actually nullifies the smooth-line character and leads to difficult theoretical problems of reflections.

SOLUTIONS FOR THE FINITE DISTORTIONLESS LINE

Although, for economic reasons, actual lines are rarely truly distortionless, many transient problems can be solved with significant approximation to the actual case by assuming $\sigma=0$. The simplifications obtained and the clearer physical interpretation possible amply compensate for the admitted deviation from accuracy.

Taking the general boundary conditions as formulated in equations 20 and 21 and combining them with the general solutions 17 and 18 one obtains for the case of de-energized initial state of the line $[i_0(x)=e_0(x)=0]$ values for the integration constants which involve the ratios

$$k_0=\frac{\mathcal{Z}_c-\mathcal{Z}_0}{\mathcal{Z}_c+\mathcal{Z}_0},\qquad k_d=\frac{\mathcal{Z}_c-\mathcal{Z}_d}{\mathcal{Z}_c+\mathcal{Z}_d} \tag{39}$$

and lead to the final forms

$$E(x, p)=\frac{\mathcal{Z}_c}{\mathcal{Z}_c+\mathcal{Z}_0}\,\frac{\exp(-\gamma x)-k_d\exp[-\gamma(2d-x)]}{1-k_0k_d\exp(-\gamma 2d)}\,Le_a(t) \tag{40}$$

$$I(x, p)=\frac{1}{\mathcal{Z}_c+\mathcal{Z}_0}\,\frac{\exp(-\gamma x)+k_d\exp[-\gamma(2d-x)]}{1-k_0k_d\exp(-\gamma 2d)}\,Le_a(t) \tag{41}$$

From equation 19 with $\sigma=0$ the characteristic impedance

$$\mathcal{Z}_c=\sqrt{\frac{l}{c}}=R_s \tag{42}$$

becomes a pure resistance, the "surge resistance"; one therefore expects the simplest forms to occur for terminal resistances $\mathcal{Z}_0=R_0$ and $\mathcal{Z}_d=R_d$ because then all the coefficients in equations 40 and 41 reduce to numerical ratios, independent of p. Assuming this special case and expanding the solutions 40 and 41 by direct division, one has

$$\begin{aligned}E(x, p)=\frac{R_s}{R_s+R_0}\,[Le_a(t)]\,[&\epsilon^{-\gamma x}-k_d\epsilon^{-\gamma(2d-x)}+k_0k_d\epsilon^{-\gamma(2d+x)}-\\&k_0k_d^2\epsilon^{-\gamma(4d-x)}+k_0^2k_d^2\epsilon^{-\gamma(4d+x)}-\dots]\end{aligned} \tag{43}$$

$$\begin{aligned}I(x, p)=\frac{1}{R_s+R_0}\,[Le_a(t)]\,[&\epsilon^{-\gamma x}+k_d\epsilon^{-\gamma(2d-x)}+k_0k_d\epsilon^{-\gamma(2d+x)}+\\&k_0k_d^2\epsilon^{-\gamma(4d-x)}+k_0^2k_d^2\epsilon^{-\gamma(4d+x)}+\dots]\end{aligned} \tag{44}$$

The first terms of the expansion for current and voltage are identical with equations 23 and 24 respectively, if one observes equation 42 and chooses $R_0=0$. Therefore, these terms constitute waves propagating out on the finite line as if it were infinitely long; this is natural since the first wave is modified only upon arriving at the termination. Since with equation 36

$$[Le_a(t)]\cdot\epsilon^{-\gamma x}=\epsilon^{-p\frac{x}{v}}\cdot\epsilon^{-p\frac{x}{v}}\,Le_a(t) \tag{45}$$

the inverse Laplacian transform is found directly by means of pair 207 of the CF-tables

$$L^{-1}[\epsilon^{-p\frac{x}{v}}Le_a(t)]=e_a\left(t-\frac{x}{v}\right)\qquad t>\frac{x}{v} \tag{46}$$

The first positive (outgoing) waves of voltage and current are therefore

$$e_{1\,\mathrm{pos}}(x, t)=\frac{R_s}{R_s+R_0}\cdot\epsilon^{-p\frac{x}{v}}\cdot e_a\left(t-\frac{x}{v}\right)\qquad t>\frac{x}{v} \tag{47}$$

$$i_{1\,\mathrm{pos}}(x, t)=e_{1\,\mathrm{pos}}(x, t)/R_s \tag{48}$$

No matter what the form of the applied voltage $e_a(t)$, both the first current and voltage waves propagate with velocity $v=\frac{1}{\sqrt{lc}}$ as exact replicas of $e_a(t)$, decreasing in amplitude exponentially along the line.[14] They arrive at the time $t=\frac{d}{v}$, at the receiving end $x=d$ with values

$$e_{1\,\mathrm{pos}}(d, t)=\frac{R_s}{R_s+R_0}\,\epsilon^{-p\frac{d}{v}}\,e_a\left(t-\frac{d}{v}\right)_{/t=d/v}\qquad i_{1\,\mathrm{pos}}=\frac{e_{1\,\mathrm{pos}}}{R_s} \tag{49}$$

Any further travel of the waves beyond $x=d$ becomes meaningless for the problem, whereas at every point for $0\leq x\leq d$ the contributions 47 and 48 to the final values persist forever. The second terms in equations 43 and 44 have the purely numerical factor $k_d<1$, so that with the proper application of equation 46 the inverse Laplacian transforms become

$$\left.\begin{aligned}e_{1\,\mathrm{neg}}(x, t)=-\frac{R_s}{R_s+R_0}\cdot\frac{R_s-R_d}{R_s+R_d}\cdot\epsilon^{-p\frac{2d-x}{v}}e_a\left(t-\frac{2d-x}{v}\right)\\ t>\frac{2d-x}{v}\end{aligned}\right\}\cdot \tag{50}$$

$$i_{1\,\mathrm{neg}}(x, t)=-e_{1\,\mathrm{neg}}(x, t)/R_s \tag{51}$$

These waves still are exact replicas of the applied voltage, but they propagate in the negative direction of x, since as t increases (and it can only increase in our actual observations) x must decrease in order to satisfy $t>\frac{2d-x}{v}$. This means that at $t=\frac{d}{v}$, both waves start at $x=d$ with reduced amplitude in the ratio

$$\frac{R_s-R_d}{R_s+R_d}$$

as compared with the arriving waves 49. If $R_d=R_s$, the waves 50 and 51 are suppressed, the finite line behaves as if it were an infinite line. Any deviation from this "matching" relation which can be generalized from equation 39 as $Z_d=Z_c$, produces the "reflection" of part of the energy in form of "returning waves" of voltage and current. Since these waves 50 and 51, though smaller in amplitude than 47 and 48, superpose upon the latter, the signal will now appear "distorted by reflection." It is therefore not sufficient to approximate the line characteristics to the distortionless case in order to obtain fidelity of transmission; it is also very important to terminate the line into an impedance Z_d close to the characteristic impedance Z_c of the line.

There are two special cases of importance, namely $R_d=0$ or short circuit, and $R_d=\infty$ or open circuit. In the first case, $e_{1\,\mathrm{neg}}$ has the same value as $e_{1\,\mathrm{pos}}$ but the opposite sign, so that the superposition results in zero voltage, whereas the corresponding current waves give double current value. In the second case, $e_{1\,\mathrm{neg}}$ has the same value and same sign as $e_{1\,\mathrm{pos}}$, so that the voltage doubles instantly, whereas the corresponding current waves cancel each other. If the incident voltage wave has a very steep front, such as that of the unit step, this doubling of the voltage, as well as the rate of rise of voltage, become important from the point of view of adequate insulation of power-transmission apparatus.

In continuation of the general discussion of solutions 43 and 44, it is apparent that the third terms again signify waves of the positive type, namely

$$\left.\begin{aligned} e_{2\,\mathrm{pos}}(x,t)&=\frac{R_s}{R_s+R_0}\cdot\frac{R_s-R_0}{R_s+R_0}\cdot\frac{R_s-R_d}{R_s+R_d}\,\epsilon^{-p\frac{2d+x}{v}}e_a\left(t-\frac{2d+x}{v}\right)\\ &\qquad t>\frac{2d+x}{v}\end{aligned}\right\}\qquad(52)$$

$$i_{2\,\mathrm{pos}}(x,t)=+e_{2\,\mathrm{pos}}(x,t)/R_s\qquad(53)$$

Again, the amplitude is reduced by the reflection factor $k_0<1$ and the waves start at $t=\frac{2d}{v}$ from $x=0$ in the positive direction; they produce further distortion by superposing upon the first two wave groups. As may be seen from the preceding discussion, the positive current waves have the same sign as the positive voltage waves, whereas the negative current waves have the opposite sign to that of the negative voltage waves.

One point is quite important to emphasize here: for $t<\frac{d}{v}$ the terms 47 and 48 constitute the *complete* and *exact* solution of the voltage-current distribution in space and time; for $\frac{d}{v}<t<\frac{2d}{v}$ the addition of the terms 50 and 51 gives the *complete* and *exact* solution; for any interval $(n-1)\frac{d}{v}<t<n\frac{d}{v}$ there are exactly n terms to give the *exact* solution. This representation of the solution by a finite and usually small number of terms is due to the special process of expansion used in equations 43 and 44, and usually called analysis in "traveling waves." Application of the theorem of residues to effect the inversion of the Laplacian transforms 40 and 41 leads to an infinite series in every case, for any interval of time; this method leads to space-time functions of the Fourier-series type and usually is called analysis in "standing waves." From the points of view of lucidity and physical significance there can be no doubt that the traveling-wave theory has the advantage, at least for phenomena in which the building-up process is of importance. For steady-state analysis, on the other hand, the standing-wave theory proves more advantageous.

ILLUSTRATIVE EXAMPLE

As an illustration, the propagation of a single square pulse of unit amplitude and of duration T, on a distortionless line with $R_d=R_0=1/2R_s$, is described. The square pulse itself can be represented by

$$[S_{-1}(t)-S_{-1}(t-T)]$$

as the difference of two unit-step functions. Since equation 39 gives here $k_0=k_d=1/3$, the general solution for the voltage as the inverse Laplacian transform of equation 43 becomes with the aid of equations 47, 50, and 52

$$\begin{aligned} e(x,t)=2/3\Bigg\{&\epsilon^{-p\frac{x}{v}}\left[S_{-1}\left(t-\frac{x}{v}\right)-S_{-1}\left(t-\frac{x}{v}-T\right)\right]-\\ &1/3\cdot\epsilon^{-p\frac{2d-x}{v}}\left[S_{-1}\left(t-\frac{2d-x}{v}\right)-S_{-1}\left(t-\frac{2d-x}{v}-T\right)\right]+\\ &1/3\cdot1/3\cdot\epsilon^{-p\frac{2d+x}{v}}\left[S_{-1}\left(t-\frac{2d+x}{v}\right)-S_{-1}\left(t-\frac{2d+x}{v}-T\right)\right]-\\ &1/3\cdot1/3\cdot1/3\cdot\epsilon^{-p\frac{4d-x}{v}}\left[S_{-1}\left(t-\frac{4d-x}{v}\right)-\right.\\ &\left.S_{-1}\left(t-\frac{4d-x}{v}-T\right)\right]+\ldots\Bigg\}\end{aligned}$$

It is apparent that the duration T of the pulse will have a decisive influence upon the shape of the signal at any point along the line. If $\frac{d}{v}=\tau$, the time of travel over the total length of the line, then suppose that $T=\frac{\tau}{2}$ or, in other words, that the pulse be a very short one. In this case all the reflections lead to individual reduced repetitions of the single applied square pulse, so-called "echoes"

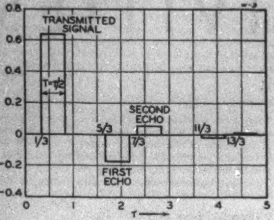


Figure 3. Voltage waves observed on a distortionless line if a square pulse of short duration is applied at the sending end

that would, of course, interfere with any other signal applied shortly after the considered pulse. Figure 3 shows the first desired signal and the various echos at the point $x=\frac{d}{3}$ from the sending end of the line. The amplitudes of the successive echoes observed at this point are:

Order Number of Wave	Positive Wave (Traveling From Sending End)	Negative Wave (Traveling Toward Sending End)
1	+0.6405	−0.1819
2	+0.0560	−0.0159
3	+0.0049	

where the attenuation is defined by $\rho\frac{d}{v}=0.12$. If, on the other hand, we suppose $T=3\tau$, or, in other words, a long square pulse applied at the sending end, then at the same point $x=\frac{d}{3}$ the shape as shown in Figure 4 would be observed. Although the individual reflections have exactly the same amplitude as previously listed, they interfere among themselves, resulting in a distorted square pulse essentially of the same duration as the applied signal but with a tail of irregular shape. A train of pulses applied at the sending end will result in considerably distorted superposition of pulses at any point along the line in spite of the fact that the line itself has been assumed distortionless.

CONCLUSION

In the space available it is of course utterly impossible to give more than an introductory survey of the most powerful method used in the analysis of traveling waves. Little has yet been published, particularly in the English language, on the application of the Laplacian transformation. The appended list of references may serve those interested further in this subject or in the more general subject of traveling waves.

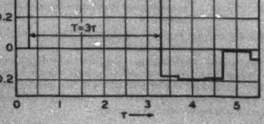


Figure 4. Voltage waves observed on a distortionless line if a square pulse of long duration is applied at the sending end

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The following references have been provided for those interested in giving more detailed study to the subjects dealt with in this article. Also a number of articles and papers concerned with these subjects, in addition to those already mentioned, have appeared recently in AIEE *Transactions* and *Electrical Engineering*.

On Laplacian Transformation

15. Theory and Application of the Laplace Transform (book in German), **G. Doetsch.** J. Springer, Berlin, 1937. Outstanding in this field; presents a rigorous and systematic treatment of the Laplace transform and its properties.

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22. Electromagnetic Theory (book), Volume II, **Oliver Heaviside.** Ernest Benn, Ltd., London, 1893.

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INSTITUTE ACTIVITIES

1942 Summer Convention Program Enlarged to Aid War Effort

With the largest summer-convention technical program in the history of the Institute, the 1942 AIEE summer convention will be held in Chicago, Ill., June 22–26. Convention headquarters will be in the Drake Hotel. Beginning with the annual meeting on Monday morning, sessions will be held both mornings and afternoons through to Friday afternoon. In addition to the annual meeting and the conference of officers, delegates, and members, the program has been enlarged to include a total of 20 sessions and conferences scheduled during the mornings and afternoons. Approximately 60 technical papers are scheduled, besides addresses and informal conference presentations. Several of the sessions and many of the papers are more or less directly related to the war effort. The complete business and technical program appears on pages 312–13. The social program will take place entirely during the evenings, except for special entertainment for the women guests arranged by the women's entertainment committee. All trips are of an educational nature and the sports events have been limited to the annual competition for the Mershon cup and the Lee trophy.

GENERAL SESSION

A feature of the convention will be the addresses by outstanding executives on the organization and management of large-scale engineering work. In communication, electrical manufacturing, or light and power, the service rendered is attained only by the carefully guided efforts and co-operation of many individuals working together in smoothly functioning units. Institute members will be able to hear this story told by three executives of long experience: M. R. Sullivan, vice-president, operating and engineering department, American Telephone and Telegraph Company, New York, N. Y.; R. C. Muir, vice-president, General Electric Company, Schenectady, N. Y.; and H. B. Gear, vice-president in charge of operating and engineering, Commonwealth Edison Company, Chicago. With the activity resulting from the war effort, an Institute session devoted to the consideration of questions relating to executive management is especially timely. The period for questions and discussion following the addresses provides an initial opportunity for greater participation by members who are more concerned with management and policies than with purely technical matters. The general session will be held on Thursday morning, June 25.

SOCIAL PROGRAM

Sunday, June 21

6:15 p.m. Reception to early convention arrivals. Music and refreshments—no charge.

Monday, June 22

9:00 p.m. President's reception. The Chicago Section will sponsor the dance following the reception.

Tuesday, June 23

6:30 p.m. "Beaches and Boulevards of Central and South America" to be held at the Lake Shore Club, where a large swimming pool and a magnificent lobby and ballroom afford excellent opportunity for portrayal of life South of the Border. Buffet dinner will be served as a preliminary to the evening's performance. Tickets for the evening, including the dinner, $1.50. No arrangements necessary for transportation, as the Club is within easy walking distance of the Drake Hotel.

Wednesday, June 24

Evening. Reservations have been made for approximately 200 convention guests to go to the Marine Dining Room at the Edgewater Beach Hotel. Applications for these tickets should be made not later than Tuesday noon. Tickets, $2.50 per person, including dinner and cover charge for the evening's entertainment. Reservations also have been made for the Adler Planetarium and applications for these should be made by Monday evening.

Thursday, June 25

Evening. Dinner-dance at the Drake Hotel. Tickets, $3.50 per person. Summer formal.

INSPECTION TRIPS

Many trips of a scientific and educational nature are being arranged, although transportation is rapidly becoming a critical matter. The transportation and trips committee expects to furnish transportation but curtailment by the Government might make necessary the cancellation of some trips or dependence solely on public transportation. On the other hand firm commitments for transportation must be made in advance and the committee requests that advance registration for trips should be made by writing to Dwight L. Smith, chairman, trips and transportation committee. The majority of inspection trips will be limited to United States citizens. Members and guests going to Chicago are reminded to take with them their birth certificates or other proof of citizenship.

Tuesday, June 23

12:30 p.m. Old Mill Garden

There are 90 acres of landscaped gardens surrounding a typical old French manor house. A large topiary garden of dwarfed Chinese elms is of particular interest. This trip is principally for women guests.

1:30 p.m. The Museum of Science and Industry

Here may be seen, by means of ingenious machines and accurate scale models, a surprisingly accurate presentation of manufacturing processes, mining methods, transportation, and power production, as well as the technique of scientific research.

2:00 p.m. The Lakeside Press of R. P. Donnelly and Sons Company

Visitors will see the printing in black and white and color of *Life* and *Time*, as well as interesting electric control mechanisms and large presses in operation.

2:00 p.m. The Clarence Buckingham Fountain

Not to mention the spectacle, the features of operation, automatic lighting, and hydraulic pumping to force the central jet skyward 135 feet above the lower basin are of interest.

Wednesday, June 24

1:30 p.m. Technological Institute—Northwestern University

Here there are extensive modern facilities for instruction and research in chemical, civil, electrical, and mechanical engineering, and the allied sciences. Among the many laboratories the high-voltage laboratory for testing under artificial weather conditions, a totally shielded radio room, and a floating soundproof room will be of especial interest.

The new building of Northwestern University Technological Institute, Evanston, Ill., here viewed from the air, will be the object of a summer convention inspection trip

2:00 p.m. Commonwealth Edison Company Calculating Board

This a-c network analyzer has been of invaluable assistance in solving complex problems of power flow, short circuits, and stability in a large metropolitan system. The board will be on display and its operation will be demonstrated in the solution of an actual problem.

2:00 p.m. Commonwealth Edison Company Drafting Room

This single room of 12,000 square feet exemplifies the modern trend in high-intensity illumination. It is the result of a thorough research and experimentation by the engineers of the company on various types of lamps, tubes, reflectors, and diffusers conducted in close co-operation with the men actually engaged in drafting work.

8:00 p.m. Adler Planetarium and Astronomical Museum

Here may be seen a miniature universe—the sun, moon, and planets threading their ways among the stars with the days and years shortened into minutes and seconds. Antique astronomical instruments are on display, in addition to a portrayal of modern astronomical methods and equipment.

Thursday, June 25

10:00 a.m. Stock Yards

By special arrangement for those who register in advance the Swift and Company packing plant may be inspected. The inspection will include the complete process of converting hogs into dressed meat. A demonstration in Martha Logan's kitchen will include instruction by the chef and home economist. The trip is being arranged especially for women guests but men may attend.

2:00 p.m. Cermak Pumping Station

Here may be seen the motors, pumps, and metal-clad switchgear of a station which has a total installed pumping capacity of 300,000,000 gallons per day.

2:00 p.m. Underwriters' Laboratories, Inc.

Approximately 5,500 manufacturers making some 35,000 different types of products submit their materials for test and inspection according to the safety standards set up by the Underwriters' Laboratories. A large part of the work is the testing of electrical materials, devices, and domestic electric appliances.

2:00 p.m. Chicago Lighting Institute

A stage lecture-demonstration dealing with the trinity of light; namely, production, control, and application of light will be given by Mr. Carl W. Zersen, manager. A tour also will be made of the industrial lighting laboratory, modern store, fluorescent clinic, modern classroom and office, as well as an all-electric five-room home.

SPORTS

The only sports events will be the annual golf competitions for the Mershon cup and the W. S. Lee trophy. The tournament for the Mershon cup will be match-play handicap, the 16 lowest scores to qualify on Monday, June 22. Matches will then be played, first round on Tuesday, second round on Wednesday, with semifinals and finals played on Thursday.

For the W. S. Lee trophy, handicap medal-play tournament, 36 holes, the competition will run concurrently with the Mershon cup tournament on Monday and Tuesday.

Competition for both trophies will be played on courses 1, 3, or 4 at the Olympia Fields Country Club—greens fee, $2.00. The club can be reached directly by frequent electric train service from the Randolph Street Station, not far from the Drake Hotel.

ADVANCE REGISTRATION

Members who have received an advance registration card should complete and return it promptly, if they have not already done so. This will permit the committee to have badges ready and prevent congestion at the registration desk upon arrival. A registration fee of $2.00 will be charged all nonmembers except Enrolled Students and the immediate families of members.

Hotel reservations should be made by writing directly to the hotel preferred.

One of the inspection trips planned for the summer convention at Chicago, Ill., will provide an opportunity to see the a-c calculating board of the Commonwealth Edison Company in operation

1941 AIEE National Prize Awards Announced

National prize awards for papers presented during 1941 have been announced by P. L. Alger, chairman of the AIEE committee on the award of Institute prizes. In selecting the prize-winning papers the committee had the recommendations of the chairmen of technical committees as to the best papers in their respective fields, except for the Branch papers. As was the case last year, no award was made for the best paper in public relations and education, as no eligible papers in that category were presented during the year. Presentation of prizes will take place at the annual meeting of the Institute on June 22, 1942, during the summer convention at Chicago, Ill.

Papers which received awards are:

Best Paper in Engineering Practice: Prize awarded jointly to Edith Clarke (A'23, M'33) and S. B. Crary (A'31, M'37) of the General Electric Company, Schenectady, N. Y., for their paper, "Stability Limitations of Long-Distance A-C Power-Transmission Systems," presented at the 1941 winter convention, January 22–26, and published in the 1941 *Transactions*, pages 1051–9. Honorable mention was awarded to L. F. Kennedy (A'37, M'39) and A. T. Sinks (A'36) of the General Electric Company, for a paper entitled "New Current Transformer for Bus Differential Protection," presented at the summer convention, June 16–20, Toronto, Ont., and published in the 1941 *Transactions*, pages 1180–7; H. C. Myers and J. H. Cox (A'25) of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., for their paper, "Excitation Circuits for Ignitron Rectifiers," presented at the 1941 summer convention, and published in the 1941 *Transactions*, pages 943–8; M. E. Strieby (M'22, F'41) and J. F. Wentz (A'24) of Bell Telephone Laboratories, New York, N. Y., for their paper, "Television Transmission Over Wire Lines," presented at the 1941 winter convention, and published in the 1941 *Transactions*, pages 1090–6; and Earle Wild (A'27, M'36) of the Commonwealth Edison Company, Chicago, Ill., for his paper, "Methods of System Control in a Large Interconnection," presented at the 1941 winter convention, and published in the 1941 *Transactions*, pages 232–6.

Best Paper in Theory and Research: Prize awarded to J. H. Hagenguth (A'28) of the General Electric Company, Pittsfield, Mass., for his paper, "Volt-Time Areas of Impulse Spark-Over," presented at the 1941 winter convention, and published in the 1941 *Transactions*, pages 803–10. Honorable mention was awarded to J. G. Trump (A'31) of Massachusetts Institute of Technology, Cambridge, and James Andrias (A'41) of the Naval Ordnance Laboratory, for their paper, "High-Voltage D-C Flashover of Solid Insulators in Compressed Nitrogen," presented at the Pacific Coast convention, August 27–29, 1941, and published in the 1941 *Transactions*, pages 986–90.

Initial Paper: Prize awarded to G. A. Matthews (A'40) of the Detroit (Mich.) Edison Company for his paper, "Power Arc-Over on Overhead Distribution Lines and Newly Developed Equipment for Protection Against Conductor Burndown From That Cause," presented at the 1941 winter convention, and published in the 1941 Transactions, pages 596–604. Honorable mention was awarded to G. B. Tebo (A'36) of the Hydro-Electric Power Commission of Ontario, Toronto, for his paper, "Measurement and Control of Conductor Vibration," presented at the 1941 summer convention, and published in the 1941 *Transactions*, pages 1188–93.

Branch Paper: Prize awarded to M. J. DeLerno (Enrolled Student) and R. T. Basnett (A'42) of Tulane University of Louisiana, for their paper "An Electronic Circuit for Determining Power-Angle Oscillations," presented at the Southern District convention, April 4, 1941. Honorable mention was awarded to W. C. Brown (application pending) of the University of Colorado for his paper "The Application of the Amplidyne as a Voltage Regulator," presented at the meeting of the University of Colorado Branch, April 9, 1941; and to W. H. Huggins (Enrolled Student) Oregon State College for his paper "Design of the Low-Frequency Characteristics of Video Amplifiers," presented at a joint meeting of the Portland Section and the Oregon State College Branch, May, 17, 1941.

Awards made by the various Districts for 1941 papers will be announced in future issues as the information becomes available.

AIEE 1942 Summer Convention

Monday, June 22

9:00 a.m. Registration

10:00 a.m. Annual Meeting

President David C. Prince, presiding

Address of welcome, **H. B. Gear**, convention chairman

Address, "Democracy in Its Struggle Looks to the Engineer." **D. D. Ewing**, head, school of electrical engineering, Purdue University

Board of directors' report (in abstract), **H. H. Henline**, national secretary

National treasurer's report, **W. I. Slichter**

Committee of tellers' report on election of officers

Presentation of president's badge; response from President-Elect Harold S. Osborne

Presentation of prizes for papers

Presentation of Lamme medal to Forrest E. Ricketts

President's address

12:00 m. Luncheon Conference of Branch Counselors

2:00 p.m. Conference on Local Engineering Councils

This conference is intended to inspire engineers to take a greater part in community civic affairs. Much may be done in the way of appraising local situations, initiating worth-while projects, and promoting long-range planning during the war and postwar period. It is believed engineers are in a position to render very important public service.

CP.* The Formation, Objectives, and Recent Work of the Illinois Engineering Council. **Frank F. Fowle**, Chicago, Ill.

CP.* The Co-ordination of Various Groups of Engineers in Massachusetts During the Preparation of a Licensing Law. **K. B. McEachron**, Pittsfield, Mass.

CP.* The Methods of Co-ordinating the Work of the Engineering Societies in the State of Iowa. **K. R. Brown**, Des Moines, Iowa

2:00 p.m. Power Generation Session and Conference

Address, "Power Supply for the War Program." **J. E. Moore**, War Production Board

Address, "Power Supply for the War Program." **E. Falck**, War Production Board

CP.* Power Facilities and Problems in South America. **R. P. Crippen**, Ebasco Services, Inc.

42-113. Frequency Control of Load Swings. **J. E. McCormack** and **R. J. Lombard**, Consolidated Edison Company of New York, Inc.

2:00 p.m. Conference on Lighting Aids to Wartime Production

The effect of good industrial illumination on increased production, reduction in waste, and prevention of accidents makes a review of new data, new standards, and new ideas on the subject important at this time when war production must be increased by every means known to the engineer. This program has been arranged to high light the part which engineered industrial lighting can play in winning the war.

CP.* What Engineered Lighting Can Do to Aid War Production. **J. O. Kraehenbuehl**, University of Illinois

CP.* Substitute Materials for Industrial Lighting Equipment. **E. D. Tillson**, Commonwealth Edison Company

CP.* Maintaining Lighting in War Industries. **A. K. Gaetjens**, General Electric Company

CP.* Good Industrial Lighting and Safety. **J. M. Roche**, National Safety Council

CP.* Activities of the Illuminating Engineering Society to Aid War Production. **H. B. Dates**, Chairman of Light in Wartime Committee, Illuminating Engineering Society

Discussion periods will follow each talk, during which the audience will be invited to present other data and to ask questions.

Tuesday, June 23

9:30 a.m. Switching Equipment

42-130. Transient Recovery-Voltage Characteristics of Electric-Power Systems. **H. P. St. Clair**, American Gas and Electric Service Corporation, and **J. A. Adams**, Philadelphia Electric Company

42-131. Practical Calculation of Circuit Transient Recovery Voltages. **J. A. Adams**, Philadelphia Electric Company, **W. F. Skeats**, General Electric Company, **R. C. Van Sickle**, Westinghouse Electric and Manufacturing Company, and **T. G. A. Sillers**, Allis-Chalmers Manufacturing Company

42-120. Transient Recovery Voltages and Circuit-Breaker Performance. **R. C. Van Sickle**, Westinghouse Electric and Manufacturing Company

42-119. Tests and Analyses of Circuit-Breaker Performance When Switching Large Capacitor Banks. **T. W. Schroeder**, **E. W. Boehne**, and **J. W. Butler**, General Electric Company

42-114. A Compressed-Air Operating Mechanism for Oil Circuit Breakers. **R. C. Cunningham** and **A. W. Hill**, Westinghouse Electric and Manufacturing Company

9:30 a.m. Communication

42-131. Regulated Rectifiers in Telephone Offices. **D. E. Trucksess**, Bell Telephone Laboratories, Inc.

CP.* Poles and Pole Treatment. **Reginald H. Colley**, Bell Telephone Laboratories, Inc.

CP.* The Combination of Probability Curves in Engineering. **R. I. Wilkinson**, Bell Telephone Laboratories, Inc.

9:30 a.m. Instruments and Measurements Session and Conference

42-110. Theoretical Possibilities of an Internally Heated Bimetal Type of Thermal Watt-Demand Meter. **Edward Lynch**, General Electric Company

42-123. A New Moving Magnet Instrument for Direct Current. **H. T. Faus** and **J. R. Macintyre**, General Electric Company

42-134. A New Jewel for Indicating Instruments. **F. K. McCune** and **J. H. Goss**, General Electric Company

CP.* The Inspection of Surface Finishes in Practice. **W. Mikelson**, General Electric Company

CP.* The Profilometer for Production Measurement of Surface Roughness. **John R. Wieneke**, Physicists Research Company

CP.* Surface Roughness Measurements. **Charles K. Gravley**, The Brush Development Company

2:00 p.m. Conference of Officers, Delegates, and Members

Wednesday, June 24

9:30 a.m. Lightning and Miscellaneous

42-129. Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits. **G. F. Lincks** and **C. R. Craig**, General Electric Company

42-96. Modern Impulse Generators for Testing Lighting Arresters. **Theodore Brownlee**, General Electric Company

● PAMPHLET reproductions of authors' manuscripts of the numbered papers listed in this program may be obtained as noted in the following paragraphs.

● ABSTRACTS of papers appear on pages 324–8 of this issue and pages 260–2 of the May 1942 issue of Electrical Engineering.

● PRICES and instructions for securing advance copies of these papers

42-108. Modern Cathode-Ray Oscillograph for Testing Lightning Arresters. **E. J. Wade**, **T. J. Carpenter**, and **D. D. MacCarthy**, General Electric Company

9:30 a.m. Overload Operation of Transformers and Rotating Machinery

42-98. Factors Affecting the Mechanical Deterioration of Cellulose Insulation. **F. M. Clark**, General Electric Company

42-93. Application of Apparatus and Conductors Under Various Ambient Temperature Conditions. **R. E. Hellmund** and **P. H. McAuley**, Westinghouse Electric and Manufacturing Company

42-115. Emergency Overloading of Air-Cooled Oil-Immersed Power Transformers by Hot-Spot Temperatures. **V. M. Montsinger** and **P. M. Ketchum**, General Electric Company

42-101. Emergency Overloads for Oil-Insulated Transformers. **F. J. Vogel** and **T. K. Sloat**, Westinghouse Electric and Manufacturing Company

42-124. Motor Insulation, Heat, and Moisture. **P. H. McAuley**, Westinghouse Electric and Manufacturing Company

9:30 a.m. Conference on Electronic Control of Resistance Welding

The objective of this conference will be to obtain an informal interchange of opinions and experiences on various types of electronic control systems for resistance welding. It is not planned to extend the discussion to control systems for arc welding or to deal in any way with the phenomena of the welding process itself. It is hoped that those taking part will include not only engineers experienced in the manufacture of the con-

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*CP: Conference presentation; no advance copies of papers available; not intended for publication in *Transactions*.

Business and Technical Program

trol apparatus or the welding equipment itself but also the users of this equipment. The conference will be opened by brief talks by two discussion leaders, **G. W. Garman**, General Electric Company, and **E. H. Vedder**, Westinghouse Electric and Manufacturing Company, after which discussion from the floor will be in order.

12:30 p.m. Board of Directors' Luncheon Meeting

2:00 p.m. Protective Relays

42-116. Current and Potential Transformer Standardization. Subcommittee on Current Transformers

accompany the abstracts. Mail orders are advisable, particularly from out-of-town members, as an adequate supply of each paper at the convention cannot be assured. Only numbered papers are available in pamphlet form.

● ALL PAPERS regularly approved by the technical program committee ultimately will be published in Transactions; many will appear also in Electrical Engineering.

42-125. A New Single Phase-to-Ground Fault Detecting Relay. **W. K. Sonnemann**, Westinghouse Electric and Manufacturing Company

42-136. Protection of Pilot-Wire Circuits. **E. L. Harder** and **M. A. Bostwick**, Westinghouse Electric and Manufacturing Company

42-90. Factors Which Influence the Behavior of Directional Relays. **Troy D. Graybeal**, University of California

2:00 p.m. Lightning

42-91. Practical Design of Counterpoise for Transmission-Line Lightning Protection. **E. Hansson** and **S. K. Waldorf**, Pennsylvania Water and Power Company

42-107. Study of Driven Rods and Counterpoise Wires in High-Resistance Soil on Consumers Power Company 140-Kv System. **J. G. Hemstreet**, Consumers Power Company, and **W. W. Lewis** and **C. M. Foust**, General Electric Company

42-103. Induced Voltages on Transmission Lines. **C. F. Wagner** and **G. D. McCann**, Westinghouse Electric and Manufacturing Company

42-109. Abnormal Currents in Distribution Transformers Due to Lightning. **J. M. Bryant** and **M. Newman**, University of Minnesota

42-104. Effect of Lightning on Thin Metal Surfaces. **K. B. McEachron** and **J. H. Hagenguth**, General Electric Company

2:00 p.m. Electronics and Standards

42-87. Current Ratings of Electronic Devices for Intermittent Service. **R. E. Hellmund**, Westinghouse Electric and Manufacturing Company

42-94. Analytical Treatment for Establishing Load-Cycle Ratings of Ignitrons. **D. E. Marshall** and **E. G. F. Arnott**, Westinghouse Electric and Manufacturing Company

42-122. Electronics of the Fluorescent Lamp. **M. A. Townsend**, General Electric Company

42-106. Sealed-Tube Ignitron Rectifiers. **M. M. Morack** and **H. C. Steiner**, General Electric Company

Thursday, June 25

10:00 a.m. General Session—The Organization and Management of Large-Scale Engineering Work

Address, "The Organization of Large-Scale Engineering Work." **M. R. Sullivan**, vice-president, operating and engineering department, American Telephone and Telegraph Company

Address, "The Engineering Organization of a Large Industrial Business." **R. C. Muir**, vice-president, General Electric Company

Address, "Engineering as an Implement of Management." **H. B. Gear**, vice-president in charge of operating and engineering, Commonwealth Edison Company

Period for questions and discussion

2:00 p.m. Power Transmission and Distribution

42-88. Analysis of Short Circuits for Distribution Systems. **Charles F. Dalziel**, University of California

42-112. Series Capacitors for Transmission Circuits. **E. C. Starr**, Oregon State College, and **R. D. Evans**, Westinghouse Electric and Manufacturing Company

42-117. Stability Study of A-C Power-Transmission Systems—I and II. **John M. G. Holm**, Boston, Mass.

2:00 p.m. Land Transportation

42-137. Electrical Facilities and Operating Plan for the First Chicago Subway. **Charles E. DeLeuw**, Chicago Department of Subways and Superhighways

42-121. Sleet Problems on Electrified Railroads. **H. F. Brown**, New York, New Haven, and Hartford Railroad

42-126. Improvements in Preventive Coil Control for A-C Locomotives With Particular Reference to "Resistor Transition." **P. H. Hatch**, New York, New Haven, and Hartford Railroad Company, and **H. S. Ogden**, General Electric Company

42-127. Electric Control for Steam Boilers on Diesel and Straight Electric Locomotives. **E. H. Burgess**, Vapor Car Heating Company, Inc.

2:00 p.m. Conference on Educational Matters Under War Conditions

Friday, June 26

9:30 a.m. Mercury-Arc Rectifier Applications

42-138. Electrical Equipment for Large Electrochemical Installations. **T. R. Rhea** and **H. H. Zielinski**, General Electric Company

42-118. Ignitron Rectifiers in Industry. **J. H. Cox** and **G. F. Jones**, Westinghouse Electric and Manufacturing Company

42-105. Ignitor Excitation Circuits and Misfire Indication Circuits. **A. H. Mittag** and **A. Schmidt, Jr.**, General Electric Company

42-139. A New Multiple High-Speed Air Circuit Breaker for Mercury-Arc-Rectifier Anode Circuits and Its Relation to the Arc-Back Problem. **J. W. Seaman** and **L. W. Morton**, General Electric Company

42-99. A 600-Volt Enclosed Limiter for Network Use. **P. O. Langguth**, **H. L. Rawlins**, and **J. M. Wallace**, Westinghouse Electric and Manufacturing Company

9:30 a.m. Basic Sciences

42-140. On Eddy Currents in a Rotating Disk. **W. R. Smythe**, California Institute of Technology

42-128. The Effect of Initial Conditions on Subharmonic Currents in a Nonlinear Series Circuit. **Stephen J. Angello**, Westinghouse Electric and Manufacturing Company

42-141. Energy Flow in Electric Systems—The VI Energy-Flow Postulate. **Joseph Slepian**, Westinghouse Electric and Manufacturing Company

42-142. Formulas for Calculating Short-Circuit Stresses in Bus Supports for Rectangular Tubular Conductors. **Thomas J. Higgins**, The Tulane University of Louisiana

Transients in Circuits Having Distributed Characteristics. **Wilbur R. LePage**, Radio Corporation of America

12:30 p.m. Luncheon—Presentation of Sports' Prizes

2:00 p.m. Selected Subjects

42-97. The Carbon Arc—A Valuable Industrial Tool. **W. C. Kalb**, National Carbon Company, Inc.

42-143. Electrical Features of Design and Operation of the Plantation Pipe Line. **M. A. Hyde**, Westinghouse Electric and Manufacturing Company, and **H. B. Britton**, Plantation Pipe Line Company

42-100. Field Harmonics in Induction Motors. **M. M. Liwschitz**, The Polytechnic Institute of Brooklyn and Westinghouse Electric and Manufacturing Company

42-132. Precision Speed Control for World's Largest Induction Motor. **R. R. Longwell** and **M. E. Reagan**, Westinghouse Electric and Manufacturing Company

42-89. Steady-State Theory of the Amplidyne Generator. **Troy D. Graybeal**, University of California

2:00 p.m. Cables

42-111. Load Ratings of Cable—II. **Herman Halperin**, Commonwealth Edison Company

42-102. Low-, Medium-, and High-Pressure Gas-Filled Cable. **G. B. Shanklin**, General Electric Company

42-133. 120-Kv Compression-Type Cable. **I. T. Faucett** and **R. W. Atkinson**, General Cable Corporation, and **L. I. Komives** and **H. W. Collins**, The Detroit Edison Company

42-135. 120-Kv High-Pressure Gas-Filled Cable. **I. T. Faucett** and **R. W. Atkinson**, General Cable Corporation, and **L. I. Komives** and **H. W. Collins**, The Detroit Edison Company

42-92. The Dielectric Strength and Life of Impregnated Paper Insulation—III. **J. B. Whitehead**, The Johns Hopkins University

North Eastern District Proves Wartime Value of Technical Conclave

The outstanding success of the North Eastern District meeting held April 29–May 1, 1942, at the Van Curler Hotel in Schenectady, N. Y., should serve emphatically to set at rest the questions that have been raised as to the advisability of continuing AIEE meetings and conventions during prevailing wartime conditions. All technical and general sessions were notably well attended, and the total verified registration of 481 is the highest in more than ten years. An analysis of attendance and comparison with previous years are given in the accompanying tabulations. Attendance at technical sessions, including parallel sessions, ranged from 65 to 170, averaged nearly 100.

VICE-PRESIDENT LEE SOUNDS KEYNOTE

Everett S. Lee, AIEE vice-president from the North Eastern District, opened the first session with the following statement, keynoting the objectives of the meeting:

"In the midst of a busy year of war production we have been planning for this meeting that it may contribute usefully to us, both in the present and for the future; in the present that from the assembling of ourselves together we may help to bring to the great engineering task before us now, of providing unprecedented materials for war, the same supremacy to which meetings such as this have previously contributed in time of peace; and to the future, that as we restore the peace it will be permanent.

"To this end the program of this meeting has been prepared. You will find in it a picture of the engineer in war and in the peace to come. You will find in it the contributions of the engineer which have made available to all people the services of electricity that they daily use in their ever-expanding life.

"Every citizen is familiar with the telephone and the radio; frequency modulation is here. and television is to come. You will find these in our program.

"Every citizen knows of electric service, of the electric light, of the electric motor. In our program you will find the results of that great search for even more efficient electric-power production in the mercury turbine that there may come to each citizen of the United States greater use of electricity. And together with this you will find the opportunities for better distribution of that same power with other opportunities for its more effective utilization.

"Every citizen today knows of the need for conserving materials and that materials once abundant must now be replaced with others. No one knows this better than the engineer, for it is he who is on the firing line of production and who must scheme out new ideas for conserving critical materials and for using substitutes. You will find these in our program.

"Every citizen today knows of the blackout, and it is the engineer who must face and solve a multitude of problems that large blocks of power and individual uses may be adequately maneuvered. You will find these in our program.

"Every citizen today knows of the need for aluminum for war production and you will find in our program the mercury-arc rectifier, that product of research and engineering which is making the precious aluminum available today in quantities beyond all previous thought.

"Every citizen today knows of the new trains that speed across our country. You will find these in our program.

"And as every citizen recognizes the need for the greatest of efficiency in manufacturing operations together with control of quality of product, so you will find these in our program.

"Every citizen is interested in education and you will find in our program a most comprehensive opportunity for the evaluating of that great field of vocational training to which the present owes so much and to which the future will without a doubt be even the greater debtor. And likewise our fundamental interest in engineering students which has always been great, you will find in our program.

"And so in the midst of this busy year of war production we meet to consider matters of vital interest to each citizen of our country."

The objectives indicated by Mr. Lee were implemented by the heaviest program of papers and addresses ever to be scheduled for a District meeting. This program included 12 regular technical program papers, 12 special addresses, 19 informal or "conference papers," 5 student technical papers, and several unscheduled contributions at the various conferences.

ATTENTION FOCUSED ON WAR PROBLEMS

The featured speakers at the opening general session further keynoted the objectives of the meeting. Executive Engineer L. A. Hawkins of the General Electric research laboratory dealt with the important role being played by "wartime electrical research," pointing out how the laboratory is making contributions vital to the successful prosecution of modern mechanized warfare, and, incidentally developing many things which probably will be highly useful in the new peace for which the present fight is being waged. Vice-President R. C. Muir of the General Electric Company described "Wartime Electrical Engineering," supplementing Mr. Hawkins' address by indicating the diversity of the contributions being made by the electrical engineer in the discharge of his obligations incidental to the current situation. The full text of both these addresses will be found elsewhere in this issue of *Electrical Engineering*.

Supplementing the addresses just noted, AIEE President D. C. Prince spoke on "Planning for Peace," showing the necessary and vital interrelationship between the necessity for the present all-out effort in prosecuting the war to a successful conclusion, and the necessity for also currently considering and planning for the proper handling of vital details of the postwar problem that will constitute the foundation upon which the peace being fought for must ultimately rest. The essential substance of his Schenectady address, as well as other matters of current significance and importance, is expected to be incorporated in a special message currently in preparation by President Prince for presentation to the Institute membership through the medium of *Electrical Engineering*, tentatively the forthcoming July issue. National Secretary H. H. Henline's address on "AIEE Progress" will be found elsewhere in these columns.

Analysis of Registration at Schenectady

Classification	Schenectady Section	District 1*	Other Districts	Totals
Members	143	54	59	256
Enrolled Students	28	49	9	86
Men guests	37	37	25	99
Women guests	27	4	9	40
Totals	235	144	102	481

* Outside Schenectady.

North Eastern District Meeting Attendance 1932–1942

Date	Location	Attendance
1942—Apr. 29–May 1	Schenectady, N. Y.	481
1941—Apr. 30–May 2	Rochester, N. Y.	355
1939—May 3–5	Springfield, Mass.	439
1938—May 18–20	Lenox, Mass.	417
1937—May 5–7	Buffalo, N. Y.	352
1936—May 6–8	New Haven, Conn.	310
1934—May 16–18	Worcester, Mass.	337
1933—May 10–12	Schenectady, N. Y.	431
1932—May 4–7	Providence, R. I.	252

Currently significant developments in electronics and communication, problems and procedures involved in the conservation of critical materials and in the development and use of substitutes for those materials, power-supply problems incidental to protective black-outs, and quality control in the mass production of war goods typify the topics of direct war significance covered in various technical sessions and conferences. Some of these are reflected in this issue; some will be published or digested in subsequent issues; some must be withheld from publication.

VOCATIONAL EDUCATION DISCUSSED

With vocational education a subject of strategic importance to an industrial nation engaged in a highly mechanized war, the topic appropriately was given special attention at the Schenectady meeting. In addition to student activities and related discussions of educational topics, a joint luncheon and subsequent joint conference on the subject of vocational education was held with the participation of the local Schenectady section of The American Society of Mechanical Engineers.

Speaker at the joint luncheon was Oakley Furney, teacher of industrial and technical education, Office of the New York State Commissioner of Education at Albany; his topic, "Needs and Trends in Vocational Education." Mr. Furney defined the term "vocational education" as meaning technical training of less than the college-graduate professional level. He voiced the conviction that quite aside from the unusual demands currently growing out of industrial war effort, properly oriented vocational education is essential to the proper balance of the modern program of public instruction through high schools if such is to serve effectively the needs of a 20th-century society. This society needs a large supply of persons educated and trained to the level of high-school graduation in technical, manual, and industrial subjects just as much as in academic or cultural subjects. Mr. Furney strongly decried the oft-recurring tendency to regard vocational education literally as a dumping ground for the mentally and physically unfit. As basic requirements for the success of an effective vocational education program, Mr. Furney outlined the following points:

1. A clearly defined objective.

2. Properly qualified students, carefully selected on the basis of the interest, aptitude, and physical and mental qualifications of each individual. Vocational training as an educational objective must be separated and distinguished from the sociological problem of rehabilitating persons of impaired mental or physical qualities.

3. Administrative officers and teachers must have a background of practical professional experience, to enable them to pass along to students a necessary practical grasp of practical problems. This is no place for academicians.

4. Machines and equipment must be typical of those with which the students will have to deal in actual life. Miniatures, toys, and laboratory models are utterly inadequate.

5. The course of study must be properly planned and carefully organized so that it constitutes a well-integrated pattern through which the stated objective may be achieved.

Aside from the topic of vocational education, Mr. Furney expressed his own conviction that the high-school educational program in general would better serve the needs of modern society if it were to be reorganized to devote the first three years for all students to a common basic course, with the fourth year to provide the specialized courses necessary to prepare properly the different groups of students that would be aiming toward college, shop, trade, or commercial careers, as the case might be.

Local Initiative Vital to Education. At the afternoon conference, Doctor Alonzo Grace, Commissioner of Education for the State of Connecticut, challenged his audience by reminding them that, for success in an educational program in a democracy, as for many other things, local initiative and the agressive assumption of local responsibility is vital. Also, he criticized the concept of education that puts social premium on purely intellectual courses of study, followed in progressively degraded order by commercial, industrial, and trade applications. He urged the development of a concept that would recognize the essentially common level of value to the community of different courses, each of which helped differently qualified persons to take their appropriate and necessary places in the over-all life of the community. It is expected that Doctor Grace's address will be available for early publication in *Electrical Engineering*.

Superintendent W. H. Pillsbury of the Schenectady school system spoke in defense of a composite high school for a city of 100,000 or less inhabitants, as against the completely segregated and specialized technical or vocational-training school envisioned by Mr. Furney. Mr. Pillsbury expressed the belief that the "best students could be given the best training" if academics and arts courses were given at the same institution, each given by a properly qualified and experienced specialist. He expressed the belief that such an arrangement permitted pupils to switch from academic to technical or trade courses without the possibility of implied stigma that might be applied to pupils transferring from an academic high school to an entirely separate trade or technical high school, and vice versa.

A comprehensive description of the vocational-training program being given with eminent success at the Mont Pleasant Technical High School in Schenectady was given by Principal C. E. Crofoot of that school. A corresponding article by Mr. Crofoot is scheduled for publication in an early issue of *Electrical Engineering*.

STUDENT ACTIVITIES

Student activities, always a major feature of North Eastern District meetings, held a prominent place in the Schenectady program. A technical session was devoted to the presentation of student papers. A conference of Student Branch chairmen, including incoming and outgoing chairmen, was devoted to an exchange of experiences and a discussion of ways and means of improving the effectiveness of Student Branch activities. This latter was strictly a student affair. Also, there was a separate conference of Student Branch counselors, held for the purpose of clearing matters of routine business. The students present at Schenectady represented 14 out of the 18 Branches in the District. Of the Branch counselors 9 were present.

At a luncheon meeting heavily attended by students and others, M. M. Boring of the General Electric Company spoke on "The Effect of War on Young Engineers Inducted Into Industry." As Mr. Boring's remarks are considered to be of especial significance and value to young engineers in the present wartime situation, the essential substance of his address is published elsewhere in this issue.

AIEE President Prince and National Secretary Henline each spoke briefly, and Past President Charles F. Scott briefly mentioned some points of historical interest concerning the early development of Institute Sections and Branches. Honor guests included Vice-Presidential Nominee Karl B. McEachron, and A. L. Rohrer who about 50 years ago laid the foundation for the General Electric Company's present "student course" for engineering graduates. Vice-President Everett S. Lee spoke briefly on the significance and importance of student participation in engineering activities through the medium of such organizations as AIEE. Mr. Lee also presented District student-paper prizes to E. F. Lapham, Jr., S. B. Cohn, and J. N. Hines for the presentation of their papers as listed in the following program.

STUDENT TECHNICAL PROGRAM

The student technical session, held Friday morning, was attended by more than 100 persons. The program included the following five papers:

1. Summer Work and the Student Engineer. **Joseph F. Furlong, Jr.,** Union College.

2. Short-Circuit Transients in Transformers. **Charles Ewaskio,** University of Connecticut.

3. An Amplidyne-Controlled Diesel-Electric Generator. **J. Ned Hines,** University of Connecticut.

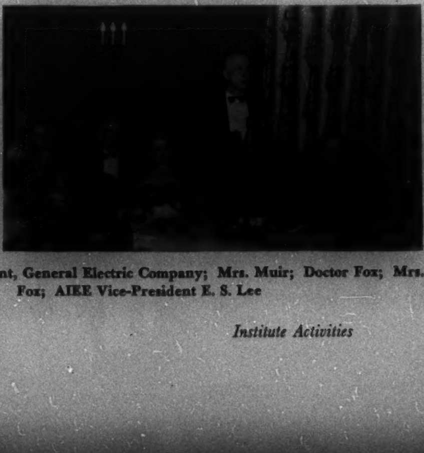
This paper was awarded third prize for presentation.

4. A New Type of Automatic Radio Direction Finder. **Seymour B. Cohn,** Yale University.

This paper was awarded second prize for presentation.

5. A Wartime Plan for Reclaiming Lead-Covered Cable.. **Edmund F. Lapham, Jr.,** Northeastern University.

This paper was awarded first prize for presentation.



Doctor D. R. Fox, president of Union College, Schenectady, N. Y., addressed the banquet held during the AIEE North Eastern District meeting. Left to right are: T. M. Linville, chairman-elect of the Schenectady Section; Mrs. Linville; R. C. Muir, vice-president, General Electric Company; Mrs. Muir; Doctor Fox; Mrs. Fox; AIEE Vice-President E. S. Lee

DISTRICT EXECUTIVE COMMITTEE MEETS

In addition to closing items of routine business for the current year, the District executive committee at a dinner meeting Thursday evening, May 1, briefly discussed some tentative plans and proposals for next year's District meeting and Student Branch convention. A suggestion to hold the Student Branch convention in New York City during the 1943 winter convention, possibly as a joint affair in collaboration with the New York City District and other near-by Districts, was very favorably received. The proposal is to be referred to the student counselors and to the 1942–43 District executive committee for final action. The expedited wartime educational program now being pursued by many of the technical schools and colleges in the District contributed to the favorable consideration of this proposal. Many colleges will be graduating seniors in January or February, and again in June or July, thus adding complications to the usual procedure of holding a student convention in the Spring.

The meeting was attended by the following representatives:

Everett S. Lee, vice-president, AIEE
R. G. Lorraine, secretary, North Eastern District
E. A. Walker, chairman, District committee on student activities
E. B. Alexander, chairman, Niagara Frontier Section
R. F. Chamberlain, chairman, Ithaca Section
A. G. Conrad, secretary, Connecticut Section
A. L. Davis, secretary, Springfield Section
G. M. L. Sommerman, chairman, Worcester Section

Also in attendance were Past President Charles F. Scott, Past Vice-President A. C. Stevens, Vice-President-Elect K. B. McEachron, National Secretary H. H. Henline, Editor G. Ross Henninger, Secretary H. G. Smith of Ithaca Section, and Victor Siegfried, District vice-chairman of the national membership committee.

DOCTOR FOX ADDRESSES BANQUET

The District's annual banquet, reception, and dance, the only general entertainment affair during the meeting, was held Friday evening at the Mohawk Club, with 283 persons attending. At the close of the banquet, Doctor D. R. Fox, president of Union College, Schenectady, addressed the gathering on the assigned topic "Technical Arts in the Life of the Engineer." Supplementing this, AIEE President D. C. Prince spoke on "The AIEE in the Life of the Engineer." Vice-President Everett S. Lee presided, and also presented to T. A. Rich (A'41) a certificate and honorarium covering the District initial-paper prize for the calendar year 1941 for his paper "Ampere-Squared-Second Recorder" which was presented at the North Eastern District meeting at Rochester, N. Y., May 2, 1941. Mr. Rich had the rare distinction of having his paper rated also as the best paper of the year against all contenders, as well as the best initial paper of a new author.

The other entertainment features provided by the local committee included a stag dinner Wednesday evening at which 247 were present. The double-header feature of the entertainment program for this dinner included an excellent burlesque of the "Doctor Quiz" type of radio program currently so popular, complete with four stooges scattered through the audience with portable microphones for the purpose of placing selected victims on the receiving-end of trick questions; and the competitive performance of four male quartets, each of which first sang one song and then led the audience in a second song, the volume of which was recorded by a noise-measuring device for the purpose of giving a competitive rating to the various quartets. All talent for the show was recruited from the membership of the Schenectady Section, and the performance was highly entertaining. Doctor Simon Ramo of the Schenectady Section served as master of ceremonies, pausing for this purpose before dashing off across the continent to address the San Francisco Section and others on the subject of ultrahigh frequency developments.

An excellent program for the women who were the guests of the local women's committee, included a luncheon at the Gideon Putnam Hotel at Saratoga Springs and a visit to WRGB television studios Wednesday afternoon; a trip to the Mohawk Carpet Mills at Amsterdam, N. Y., Thursday noon, where an excellent luncheon was given by the Mills; a tour through the Union College campus Friday morning, and tea and music that afternoon at the home of Mrs. Harry A. Winne.

Inspection trips, as a result of wartime regulations, were limited to the WRGB Television Studio, the Mont Pleasant High School, and the Schenectady General Depot of the United States Army. A "House of Magic" program entitled "New Developments" was presented Thursday afternoon by the General Electric Company in its Rice Hall to such persons as could prove that they were United States citizens.

STEINMETZ MEMORIAL LECTURE

Coincidental with the District meeting, the 15th in the series of Steinmetz memorial lectures annually arranged under the auspices of the Schenectady Section was held Thursday evening April 30 in Memorial Chapel at Union College, and attended by a large number of the District meeting registrants. Lecturer this year was Doctor Comfort A. Adams, past president of the Institute (1918–19) and AIEE Lamme Medalist (1940). Doctor Adams lectured on the topic "Co-operation Versus War."

The lecture series was inaugurated in 1925 by the AIEE Schenectady Section as a living and recurrent memorial to Doctor Charles Proteus Steinmetz, past president (1901–02) of the AIEE and for many years eminent scientist of the General Electric Company. The costs of the memorial are met by the income from an endowment fund contributed by friends and admirers of Doctor Steinmetz.

National Secretary Outlines Institute's Development and Trends

That the engineering profession offers a great variety of opportunities for self-development and for vital service to the public was re-emphasized by National Secretary H. H. Henline in a brief address delivered before the opening general session of the recent North Eastern District meeting in Schenectady, N. Y. The essential substance of Mr. Henline's outline of his assigned topic, "AIEE Progress," is given in the following paragraphs.

The membership of the Institute on April 1, 1942, was 18,999, the highest ever reached. Although the loss from the predepression peak was 22 per cent, the turning point came suddenly in 1935, and since that time the membership has increased at a satisfactory rate. The Institute has been the largest of the engineering societies since 1937.

During the depression, the income of the Institute was reduced by 43 per cent below

About half of the guests at the dinner dance, entertainment feature of the North Eastern District meeting, are shown on the lawn of the Mohawk Golf Club, Schenectady, N. Y.

the income for the year 1927–28. Through reductions of expenditures and the use of a moderate amount from the reserve capital fund, the activities were well maintained. In recent years the finances have recovered at a satisfactory rate and restorations to the reserve capital have been made accordingly. The income for 1940–41 was only 12 per cent below that for 1927–28, and activities are being maintained at a high level that is in many respects even more effective than in 1927–28.

NEW COMMITTEES

Continuing efforts are made to keep the Institute's technical-committee structure up to date and in keeping with the needs of the era. Thus, in 1938 three of the technical committees (applications to iron and steel production, applications to mining work, general power applications), were combined to form a single strong committee on industrial power applications. Since that time the fields previously served by the committee on transportation and the committee on applications to marine work were reallocated and three technical committees were formed to replace them: one on air transportation, one on land transportation, and one on marine transportation. To meet growing needs in other directions, the technical committees on domestic and commercial applications and application of electricity to therapeutics were formed in 1940 and 1941, respectively.

Also, in recognition of the fact that research is an activity extending into every major branch of the electrical field where technical advances are being made, the long-standing committee on research has been transferred from the status of a technical committee to that of a general standing committee. In 1941, the committees on economic status of the engineer, and legislation affecting the engineering profession, were combined into the Institute policy committee, which has a broad statement of scope in the bylaws and is performing the functions of the first two committees through subcommittees.

MEETINGS, SECTIONS, BRANCHES

The Institute has been holding three national conventions and from two to four District meetings per year with excellent attendance and keen interest. Recently, there has been discussion of the desirability of curtailing these meetings on account of the war situation. However, it should be noted that the Vancouver Section, which had been authorized to hold the 1940 Pacific Coast convention and requested that it be relieved from this responsibility after Canada entered the war, has requested and received authority to hold the 1942 Pacific Coast convention. Plans for the summer convention indicate a full technical program and the board of directors voted to make it a "working convention." The policy is to continue normal schedules of meetings unless some good reason for curtailment appears. [*Editor's Note:* Results of this District meeting at Schenectady provide emphatic support for continuing such meetings.]

Technical papers of the Institute are useful in wartime as well as in peacetime. Standard No. 45, covering Electrical Installations on Shipboard, was revised in July 1940, and the demand for copies has been heavy because of the great shipbuilding program.

In recent years two or three new Sections have been formed each year, and the total now is 72. In January, the board of directors provided for including all of the territory in the United States in Section territories. Hence, any new Sections formed hereafter will represent subdivisions or reallocation of territory presently assigned to some Section. A new Student Branch recently organized at the University of Delaware brings the total number to 124.

WAR EFFORTS

In war service the Institute is taking its proper place, as circumstances develop. In accord with a resolution adopted by the board of directors in 1940, offering the services of the Institute to the President of the United States in connection with national defense, the Institute has continued co-operating at every possible opportunity. It is, of course, realized that the specific services which any engineering society can render as an organization are small compared to the collective total of the services rendered by its individual members in the Army, Navy, War Production Board, and other government agencies, and in all manner of industries which are producing vitally necessary supplies for war needs and for the maintenance of essential civilian services.

Co-operation has been extended in many activities, including the national census of engineering construction firms, the extension of subcontracting plans, the National Roster of Scientific and Specialized Personnel, a study of supply of and demand for engineers, and others. The Institute organized a committee on national defense and a committee on civil protection, and is represented on the National Technological Civil Protection Committee and on the Engineers Defense Board. Its series of six radio programs on defense subjects presented in the winter and spring of 1941 was received with much interest. Many of the Sections have co-operated in war activities.

PROFESSIONAL ASPECTS OF ENGINEERING

The Institute is primarily a technical organization, and its objectives are based upon broad, unselfish motives. Its activities are directed toward improved personal qualifications and general progress rather than mere class advancement.

A profession has been defined as a calling in which one professes to have acquired some special knowledge used by way of instructing, guiding, or advising others, or of serving them in some art. Hence, the guiding principle of a profession is service to others.

Thoughtlessly expanding from this very broad characteristic of a profession, many persons, some of them engineers, occasionally have a tendency carelessly to compare the engineering profession with the legal or medical profession, and to indicate that they are or should be essentially the same. Engineering is very different from medicine and law. In the latter professions the nature of professional service is highly personal, largely man-to-man and confidential. Engineering, however, is a very broad field of activity; extending from the realm of science on the one hand to that of business on the other hand. Engineers operate principally as elements of large organizations—such as local and national government agencies, manufacturing industries, utilities—where they deal with things rather than with people. Hence the engineer's services, although no less real and no less vital to people, are indirect rather than direct.

It is a serious mistake to try to make engineering similar to the professions of medicine and law and to use its professional aspects for greater personal advantages. The situation in the engineering profession does not represent confusion as some engineers claim, but offers a great variety of opportunities for self-development and for vital service to the public.

Substitutions for Critical Materials Discussed at Schenectady Meeting

At a conference during the recent North Eastern District meeting in Schenectady, the general topic "Conservation of Critical Materials by the Use of Substitutes," was discussed—with reference to the telephone field by J. R. Townsend of the Bell Telephone Laboratories, Inc., New York, N. Y., and with reference to the manufacturing procedures of the General Electric Company by John Horn of the engineering general department of that company.

BELL SYSTEM PRACTICES

Shortly after the outbreak of the war in Europe in 1939, the Bell Telephone Laboratories intensified its long-standing normal project of collecting data on raw materials and conducting studies of comparative suitability of different materials for specific applications in telephone equipment. According to Mr. Townsend's report, this general program of studies had for some years been concerned with the possible use of plastics in replacing rubber and metal, and the possibility of using copper-silicon alloys to replace nickel-silver which is used for contact springs. Results of these studies have permitted quick substitutions to be made under emergency conditions as they arose, and have enabled impressive amounts of critical materials to be saved or conserved for direct war uses.

Since 1939, the need for some of this work could be anticipated; for example, substitutes for nickel, aluminum, and zinc. Soon, however, substitutes had to be found for substitutes, as the war spread to the Far East to affect such normal supplies as jute from India, tin from Malaya, felt from Australia, rubber from the Dutch East Indies, and Manila hemp from the Philippines. Since about the first of 1942, production curtailment has been necessitated by a lack of certain available materials and now the problem no longer is primarily one of using less-scarce materials, but of limiting the use of all materials to those essential to civilian economy and war necessity. Mr. Townsend pointed out that substitute materials must be abundant, easy to work, and not require special machinery or take engineering talent from more essential war duties. He also pointed out that engineering ingenuity coupled with such necessities as those currently faced frequently resulted in the development of new and sometimes less expensive products. Some permanent benefits will result.

The use of materials in wartime is based upon three groupings: those essential for the conduct of the war, those essential for necessary civilian uses, and those available as substitutes for relatively scarce materials. Respectively, aluminum, tin, and wood, are examples of these three classifications. The Bell System program for 1942 will save for war purposes 100,000,000 pounds of copper, zinc, lead, magnesium, nickel, aluminum, alloy steel, tin, chromium, crude rubber, phenol fiber, jute burlap, and silk. This is typical of the communications industry in general. Rubber, widely used in telephone practice for insulation, has been reduced by about 85 per cent. Silk, fabric and yarn, long a favorite and vital material for insulation, has been reduced by 95 per cent and is rapidly on its way toward *total* and *permanent* replacement by such substitutes as acetate fabrics, nylon, spun glass, and other materials.

GENERAL-ELECTRIC PROCEDURES

"The degree of fulfillment of the war production program depends in a large measure upon industry's ability to find and adopt substitutes for a major portion of materials which heretofore have been commonly available in ample quantities." So stated John Horn with reference to the conservation of critical materials. Further, "Coming, as it does, at a time when the call for production of tried as well as of new designs involves quantities of virtually unheard of magnitudes, the problem presents a severe challenge to the engineering profession. What is more truly an engineer's task than an everlasting endeavor to devise ways and means of meeting new requirements?" Mr. Horn described the procedure adopted by the General Electric Company as follows:

Conservation orders emanating from Washington are received at the main office in Schenectady, and there reviewed by the general manufacturing department in collaboration with the central procurement and engineering departments. This review aims principally at interpreting the orders in terms of the company at large. Accompanied by such an interpretation, the orders then immediately are passed on to the heads of production and engineering in the administrative offices of the various plants. At those places the orders become subject to a second review by local production and engineering representatives, who determine the effect of the orders upon the operations of the particular plant. Subsequently the process is repeated in all departments that will be affected. There, production and engineering representatives again go over the orders jointly and decide upon the action that may be necessary to comply with the terms of the order, and also to go beyond that point wherever it is feasible to do so in the interest of the general material situation in the country. Any appeals flow in reverse direction through the same channels for comparable co-ordination.

These procedures pertain to the General Electric Company's apparatus division, a division composed of different departments which in several instances produce similar or parallel lines of apparatus differing principally in size. With this situation, it has proved equally necessary and desirable to add on the co-ordinating effort that is provided for by going through several central points in handling both incoming regulations and outgoing appeals. Mr. Horn stated that experience to date "has definitely indicated that it is essential both to the company and to the War Production Board for conservation orders and the matters connected therewith to be handled in this truly comprehensive fashion."

As to the actual conservation efforts, where materials are commonly used throughout the manufacturing units as a whole, the work usually is organized under the leadership of one or more specialists from the company's metallurgical staff, or under the guidance of one of the several metallurgical and material committees which over a period of years have been operating as advisory and standardizing groups. These individuals or committees conduct the necessary surveys, co-ordinate the results of tests and experiments initiated to determine the adaptability of new or modified materials to existing designs and available manufacturing equipment, and also coordinate detailed local or specialized information from the various departmental or plant representatives, engineers, or manufacturing personnel. The basic information resulting from such general surveys is subsequently checked "on location" by the central co-ordinator, "to promote decisions for those changes that hold the best promise from the viewpoints of procurement, manufacturing, and engineering." Then follows the initial introduction of the new materials selected for any given process, an operation which sometimes requires preliminary operation on a trial basis to develop the necessary experience to facilitate a complete switchover in manufacturing procedure.

CONSERVATION EFFORTS CLASSIFIED

Conservation efforts may be considered as falling into two general classes: (1) conservation by substitution; (2) conservation by reduction in quantity of material used. Each of these involves its own problems, and sometimes all are interrelated.

Conservation by substitution falls into two major subdivisions: (1) a direct change from one material to another without any significant modification in design or in application; (2) a change to substitute materials that is possible only through redesign and/or more or less drastic changes in manufacturing process. A simple example of a direct change would be from primary to secondary aluminum, or from tin-base bronze to tin-free bronze. An obvious example of the type of substitution requiring design and manufacturing changes would be the shift from aluminum to steel construction. Many changes of this latter type are necessary although they commonly require extensive investment of man-hours and equipment all the way from the drafting and design board down to and including the factory floor and manufacturing machinery.

The matter of conservation by reduction in quantity of material used is largely a problem of finding new ways of using and fabricating the material in question; in other words, continuing to use the same basic material but utilizing it on the basis of material scarcity rather than on that of ease and economy of manufacture. Parts manufactured from sheet material may fall within this group, and the question may resolve itself into a different laying out of the pieces to make more efficient use of the original material, possibly resorting to the practice of welding a multiplicity of small pieces into the final desired shape as against forming from large pieces.

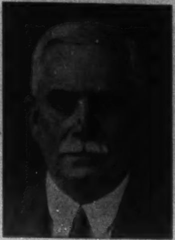
Conservation procedures obviously affect and are affected by material specifications, Mr. Horn pointed out, and require close contact and co-operation between engineers and metallurgists on the one hand and procurement specialists, vendors, and various government agencies on the other hand. In many instances the issuing of material specifications in advance form to take into account anticipated development in the material field has been found to be a very practical solution. Measures of this nature facilitate procurement and avoid unnecessary delays of shipment from suppliers who otherwise would be bound to specifications that could not be met in the present scarcity situation. Mr. Horn emphasized that "this is a point that deserves more general recognition on the part of every purchaser of material to specifications in all fields."

CRITICAL MATERIALS CONSERVED

Among a group of some 67 materials recently designated by the War Production Board as being vitally needed for war purposes and hence not generally available for other needs are the following 15 materials, all of which occupy conspicuous places in the field of electrical manufacture: alloy steel, including several of the most prominent alloying constituents; aluminum, including scrap; chromium; copper, including scrap; magnesium; nickel, including scrap; tin, including tin plate and terne-plate; tungsten, including tungsten carbide; formaldehyde; phenol-formaldehyde resins and plastics; shellac; toluol; tung oil; mica splittings; rubber, including crude, latex, chlorinated, and synthetic.

In Memoriam

BION JOSEPH ARNOLD



BION JOSEPH ARNOLD, 16th president of the Institute, died on January 29, 1942, at the age of 80.

He was born at Casnovia, Michigan, August 14, 1861, and received the degrees of bachelor of science, master of science, and honorary doctor of philosophy from Hillsdale College. He engaged in graduate study at Cornell University and the University of Nebraska. The University of Nebraska awarded him the degrees of electrical engineer, 1897, and doctor of engineering, 1911. The Armour Institute of Technology awarded him the honorary degree of doctor of science in 1907. Doctor Arnold was employed by the Chicago Great Western Railway as chief draftsman and mechanical engineer, 1887–89, and as engineer and manager of the St. Louis office, 1889–92.

He was consulting engineer in the Thomson-Houston Electric Company in Chicago, 1892–93. As an independent consulting engineer since 1893, he was engaged at various times on many railway electrification and electric traction projects.

He joined the Institute in 1892, and was transferred to the grade of Member in 1893, and to the grade of Fellow in 1912. He was elected an Honorary Member in 1937. He served the Institute as a director, 1895–98, vice-president 1902–03, and president 1903–04.

Resolved: That the board of directors, on behalf of the membership of the American Institute of Electrical Engineers, hereby expresses keen regret at the death of Doctor Arnold and sincere appreciation of his numerous contributions to the development of Institute activities during his membership of about 50 years.

Resolved: That these resolutions be entered in the minutes and transmitted to members of his family.—*AIEE Board of Directors, May 22, 1942.*

Some of these materials, of course, are more easily replaceable than others, especially those that are used for structural purposes as contrasted with those used for operational functions. Thus, the conservation effort naturally has been most effective in eliminating or reducing the consumption of structural materials.

As indicative of what the electrical industry is doing in this direction, Mr. Horn indicated the following examples of reduction in the use of aluminum: 5,000,000 pounds a year from household refrigerators, 1,300,000 pounds from electric meters, 550,000 pounds from industrial control gear, 360,000 pounds from street lighting equipment, 200,000 pounds from lightning protective devices, 165,000 pounds from switchgear construction, 70,000 pounds in the form of aluminum paint and sheet stock for name-plates, etc.

Among the remaining metals named as random selections from the WPB listing, tin comes next to aluminum in quantity consumed for structural uses. Its three main uses are tin-base bronzes, babbitts, and solders. To date, most progress has been made in reducing the tin consumption in babbitts and solders; reduction in the field of the bronzes requires the development of foundry experience with the low-tin and tin-free compositions which can be used as substitutes.

Normal composition for General Electric standard babbitt for bearings for rotating apparatus has been one containing 83.3 per cent tin. Since the beginning of January 1942, however, research has brought out a lead-base babbitt containing 82.5 per cent lead, 15 per cent antimony, 1 per cent arsenic, 1 per cent tin, and 0.5 per cent copper as a satisfactory substitute for use in centrifugally cast sleeve bearings. On that basis the manufacture of this type of bearing in the company's various motor departments is in process of changeover with a resultant expected saving of more than 200,000 pounds of tin per year at the present rate of production. Active work and some trial productions are in progress with reference to stationary-cast bearings where the conservation effort centers around a redesign to the use of thin-wall bonded bearings instead of the solid cast-in anchorage type.

Solder composition popular in recent years has been 40 per cent tin and 60 per cent lead, and compositions with a higher tin content have been used for some general-purpose work. Among the substitutes currently being experimented with, a composition of 20 per cent tin, 1 per cent silver, and 79 per cent lead has been found to be reasonably applicable with but minor changes in technique, temperatures, and operating schedules. This composition is regarded at least as a temporary replacement for the 40–60 composition, although experiments are continuing with various other compositions down to and including the no-tin composition 97.5 per cent lead, $2^{1}/_{2}$ per cent silver. For certain heavy-duty electrical joints where use has been made of 100 per cent tin, changes have been made to enable the use of silver to the exclusion of tin. A typical example of this application is commutator-riser joints on large machines. Contrary to this, however, there are some instances where adequate replacement material has not yet been discovered. One of these is in the soldering of leads to commutators, where pure tin appears to be the only material that will give safe joints under the temperatures and high speeds of actual operation. In the use of soldering pots for hot dipping, good results have been obtained with 90 per cent lead and 10 per cent tin. The wiping of cable joints represents another operation for which a definite substitute for the 40–60 solder used in the past has not yet been found. The indication is that tin solders with less than 38 per cent tin cannot be worked in a practical way to produce reliable nonplus wipes. On a trial basis, however, good results have been obtained with certain tinless solders and these now are being explored further. In general, the very substantial total requirement of tin for solders has been reduced by more than 50 per cent.

Mr. Horn mentioned the position of unique importance held by copper in the present status of electrical engineering as a whole, and stated that wherever it serves as an electrical conductor it can be replaced successfully only in rare instances. Any conservation effort on copper therefore resolves itself almost entirely into a search for the most economical utilization of copper rather than for its substitution. The various applications of copper for nonconductor uses, however, have given way largely to the use of steel, malleable iron, terne-plate, brass, bronze, and zinc, according to the requirements of the particular application.

(*Editor's Note:* As one element in the current United States effort to conserve copper, a certain new aluminum plant is scheduled to use silver throughout for all heavy-current-carrying conductors. Also, for this same job, a dozen 12,500-kva transformers are being built with windings entirely of silver conductor in place of copper. The manufacturing and operating experience obtained with this installation may disclose interesting possibilities. Qualified observers estimate, however, that the silver required for this installation represents something like 25 per cent of the government store of bulk silver bullion currently stored by the government.)

Chromium and nickel have special importance in their use as an electrical-resistance alloy in control resistors and heating elements. Where space is no element, cast-iron grids are being used as substitutes. However, many applications remain in which space is a controlling element, and for such circumstances the search to date has not yielded a practicable substitute material.

In Memoriam

RUDOLPH EMIL HELLMUND

Bachrach

ON Saturday, May 16, 1942, the electrical engineering profession suffered a great loss in the passing on of Rudolph Emil Hellmund. Born in 1879 at Gotha, Germany, Mr. Hellmund entered this country in 1903, becoming a citizen in 1920. Educated at the Technical College of Ilmenau and the University of Charlottenburg, he spent his early years as a designer of electrical machinery at Poeschmann, Cologne, and Stuttgart, Germany. In 1903, he entered the employ of the Krantz Company, of Brooklyn, N. Y., as a designer of switches and switchboards. After exercising his unusual gift as a designer in an affiliation with William Stanley and later with the Western Electric Company, he joined the staff of the Westinghouse Electric and Manufacturing Company in 1907, carrying out the development of a line of induction motors. In 1912, he was placed in charge of the design of all d-c and a-c railway machinery. Following a general assignment as consulting engineer until 1921 he then became engineering supervisor of development, chief electrical engineer in 1926, and chief engineer of the company in 1933. The award of over 300 United States and foreign patents to Mr. Hellmund and the authorship of more than 100 technical papers are striking indications of the great breadth and practicability of his engineering accomplishments. In recognition of his many contributions to machine design, he was awarded the Lamme medal in 1929.

In addition to the multiplicity of his interests in the Westinghouse Company, Mr. Hellmund always found time to take an active part in many related activities of the electrical field. He became affiliated with the AIEE in 1905 as an Associate, and in 1913 was transferred to Fellow grade. He served on many of the Institute's committees, being an active member of the Standards Committee from 1930 to 1942, acting as chairman during 1938–40. He was elected a Director of the Institute for the term 1939–1943. He served as a representative of the Institute on the Electrical Standards Committee and the Standards Council of the American Standards Association and of the United States National Committee of the International Electrotechnical Committee.

With full realization of the value of the many contributions which Rudolph Emil Hellmund made to the development of the electrical art and his sincere and unselfish devotion to the advancement of his profession, the board of directors of the American Institute of Electrical Engineers hereby directs that this minute be spread on the records and transmitted to the members of his family.—*AIEE Board of Directors, May 22, 1942.*

All the nonmetallic materials named in the WPB listing previously mentioned are components of electrical-insulation materials. Among these, formaldehyde, which is required for the manufacture of coil-treating varnishes and allied materials, is one for which no adequate substitute has been found and for which conservation is limited to careful and efficient use. Toluol is an ingredient of many varnishes of fast-drying characteristics serving as binders or adhesive agents—for instance in built-up mica; between 100 and 150 such applications common in 1941 have now been so reduced that the consumption already is down something more than 50 per cent. Substitutes for tung oil have brought about a 35 per cent reduction in current requirement.

Mica, with its long standing as the only useful material for certain electrical-insulation purposes is still an unmatched material for applications where its mechanical, heat, and other outstanding characteristics are actually needed. The grades of mica generally suitable for electrical insulation are the imported varieties from India and Madagascar, no longer available in adequate quantities. Considerable quantities of mica from deposits in both North America and South America consequently now are finding use, although the quality of this Western-Hemisphere product is lower and there is considerable difficulty in obtaining machine-splittings that compare satisfactorily with the hand-split imported grades. Selective use, in accordance with a careful grading of actual requirements, is enabling increasing use of the local product and corresponding conservation of the imported product. In one particular application, a reduction in thickness from 0.03125 inch to 0.02 inch resulted in a saving of 3,800 pounds of imported mica per year.

The subject of materials conservation is much too comprehensive to be covered in any one brief review. However, the typical procedures and examples cited indicate the enormous scope of conservation efforts currently being made, and may suggest ways for further and more rapid progress. "This is an engineering problem, the successful solution of which will promote our national welfare and interest, and will definitely help to shorten the war," stated Mr. Horn in closing.

Wartime Quality Control Discussed at Schenectady

"Statistical methods for quality control" was the general discussion topic for one of the several conferences on the program of the recent AIEE North Eastern District meeting in Schenectady, N. Y. In addition to extemporaneous discussions, two addresses were given: "Quality Control in Relation to Engineering Tolerances," by H. F. Dodge of the Bell Telephone Laboratories, New York, N. Y.; and "Quality Control and the War," by Lieutenant Colonel Leslie E. Simon of the United States Army Ordnance Department, Aberdeen, Md. Mr. Dodge spoke with particular reference to the quality-control-chart technique that has been used as an engineering tool to aid in the solution of practical problems in design and manufacture. Colonel Simon discussed the application of this control method to ordnance; the substance of his address is scheduled for publication in an early issue of *Electrical Engineering*. The essence of Mr. Dodge's remarks are reflected in the following paragraphs.

Mr. Dodge emphasized that the quality-control-chart technique has become of increasing usefulness in problems of mass production where the aim is repetitive production of a given item. The technique provides a relatively simple method of studying and controlling the variations of any quality characteristic of a product. Two principal uses of the control chart mentioned by Mr. Dodge were: (1) to assist judgment in determining whether the state of statistical control exists, and (2) to attain and maintain control of quality during production, as outlined in the "Guide for Quality Control and Control-Chart Method of Analyzing Data" published by the American Standards Association in May 1941 as one of its "American Defense Emergency Standards." Several actual examples of the application of this method were discussed in some detail. In one example, the method disclosed a relatively large variation in quality and indicated the source of the difficulty. Following the ensuing engineering investigation and corrective action, quality variation was sufficiently reduced to permit the use of still smaller tolerance limits, with a substantial saving in a costly material. In this instance, the statistical methods aided the solution, although the major contribution was made by engineering.

The second principal use of the control chart is covered in another publication ("Control-Chart Method of Controlling Quality During Production") which currently is being published by ASA for distribution early in June. Mr. Dodge pointed out that this method is recommended for use by the manufacturer as an aid in identifying and eliminating causes of trouble in repetitive processes, to reduce unnecessary variations in quality. The method is especially recommended for use where destructive testing makes 100 per cent inspection impossible, and where it is desired to exert purposive control over the percentage of rejections. In such an instance, the control chart is used for keeping a continuing record of the quality of a product while production is going on, so as to detect any assignable cause of variation as soon as it appears.

Mr. Dodge emphasized the fact that such methods are not automatic cure-alls. The work involved in the over-all control process is perhaps less than 10 per cent statistical and more than 90 per cent engineering. Thus success in application depends fundamentally upon engineering judgment, knowledge of manufacturing processes, and technical skill in tracking down the causes of trouble, the presence of which may be *disclosed* by the control chart.

NATIONAL • • • •

Series on Mathematical Analysis to Be Issued as Pamphlet

The series of articles currently being published in *Electrical Engineering* on "Advanced Methods of Mathematical Analysis as Applied to Electrical Engineering," of which the concluding article appears in this issue, is being reprinted in pamphlet form in response to demand.

The five articles were first presented as lectures before the basic science group of the AIEE New York Section at a symposium held at Columbia University during the winter of 1940–41. The symposium of which Professor Paul C. Cromwell of New York University was chairman, aroused so much interest among the members attending that the authors were asked to put their respective lectures into the form of articles in order to make them available to the entire Institute membership. The articles as subsequently published in *Electrical Engineering* are:

Heaviside's Direct Operational Calculus, J. B. Russell, Columbia University (*EE, Feb. '42, p. 84–8*)

Integration in the Complex Plane, K. O. Friedrichs, New York University (*EE, Mar. '42, p. 139–43*)

Laplacian Transform Analysis of Circuits With Lumped Linear Parameters, Jacob Millman, College of the City of New York (*EE, Apr. '42, p. 197–205*)

Analysis of Systems With Known Transmission-Frequency Characteristics by Fourier Integrals, W. L. Sullivan, Stevens Institute of Technology (*EE, May '42, p. 248–56*)

Traveling Waves on Transmission Lines, Ernst Weber, Polytechnic Institute of Brooklyn (*p. 302–09*)

The purpose of the lectures, which have been published in essentially the form in which they were delivered, was to present to engineers condensed and simplified forms of the subjects considered. For those interested in studying the basic subject matter in more rigorous and complete form, lists of references accompany each article. In order to make the series conveniently available for use as background material for round-table discussions on advanced mathematics or for similar purposes, the consolidated pamphlet reprint is being issued. Copies may be obtained from the AIEE order department, 33 West 39th Street, New York, N. Y., at $1 each; special prices will be quoted on quantity orders.

1942 Year Book Issued

The 1942 edition of the AIEE Year Book has been issued, in accordance with 1941–42 budget provisions. Addresses are corrected as of March 1, 1942. Copies have already been distributed to all national, District, and Section officers, Student Branch counselors, and all members of national committees. Other members desiring copies may obtain them by writing to the AIEE order department, 33 West 39th Street, New York, N. Y. The Year Book is not available to nonmembers of the Institute, nor is its use permitted for commercial, promotional, or other circularization purposes.

Bibliography Published on Circuit-Interrupting Devices

A second bibliography of technical literature, "Bibliography on Circuit-Interrupting Devices, 1928–1940" has just been published by the Institute. Like its predecessor, the "Bibliography of Relay Literature, 1927–1939," this special publication is sponsored by the AIEE committee on protective devices. The list of more than 850 titles includes, for the period 1928–1940 inclusive, practically all material on the subject published in the American technical and trade press and the principal articles published in other countries. All such material appearing in AIEE *Transactions* or *Electrical Engineering* is of course included.

Future AIEE Meetings

Summer Convention
Chicago, Ill., June 22–26, 1942

Pacific Coast Convention
Vancouver, B. C., September 9–11, 1942

Middle Eastern District Meeting
Pittsburgh, Pa., October 14–16, 1942

Winter Convention
New York, N. Y., January 25–29, 1943

The reference items are divided into subject sections, and within each section are numbered consecutively, subdivided by years, and listed alphabetically for each year. The subject headings are:

Circuit Breakers (Air, Oil, Water, Rapid Reclosing, Recovery Voltages, General and Miscellaneous), Enclosed Switchgear, Air Switches, Bus Bars and Fuses and Fuse Protection.

The "Bibliography on Circuit-Interrupting Devices, 1928–1940" is a 28-page $8^{1}/_{2}$ by 11-inch pamphlet, uniform in appearance with the "Bibliography of Relay Literature." It is available from AIEE headquarters, 33 West 39th Street, New York, N. Y., at 40 cents per copy to Institute members (80 cents to nonmembers), subject to a 20 per cent discount for quantities of 10 or more mailed at one time to one address. Remittances, payable in New York exchange, should accompany orders.

1942 Lamme Medal Nominations Due December 1

Special attention is directed to the fact that the names of Institute members who are considered eligible for the AIEE Lamme Medal, to be awarded early in 1943, may be submitted by any member in accordance with section 1 of article VI of the bylaws of the Lamme Medal committee, as quoted in the following:

> The committee shall cause to be published in one or more issues of *Electrical Engineering*, or of its successors, each year, preferably including the June issue, a statement regarding the "Lamme Medal" and an invitation for any member to present to the national secretary of the Institute by December 1, the name of a member as a nominee for the medal, accompanied by a statement of his "meritorious achievement" and the names of at least three engineers of standing, who are familiar with the achievement.

Each nomination should give concisely the specific grounds upon which the award is proposed, and also a complete detailed statement of the achievements of the nominee to enable the committee to determine its significance as compared with the achievements of other nominees. If the work of the nominee has been of a somewhat general character in co-operation with others, specific information should be given regarding his individual contributions. Names of endorsers should be given as specified in the foregoing quotation.

Section and Branch Activities—

The following constitutes the annual report on Institute Section and Branch activities for the fiscal year which ended April 30, 1942. Similar information for three preceding fiscal years appeared in *Electrical Engineering* for June 1941, pages 285–87; June 1940, pages 250–3; June 1939, pages 268–71.

Present members of the Sections committee and the committee on Student Branches, which supervise the two important divisions of Institute activities covered by this report, are:

Sections—M. S. Coover, *chairman*, W. B. Morton, *vice-chairman* and *secretary*, C. A. Faust, O. W. Holden, S. J. Lisberger, E. T. Mahood, R. M. Pfalzgraff, H. H. Race, J. M. Thomson, and ex officio the chairmen of all Sections of the Institute.

Branches—H. W. Bibber, *chairman*, W. J. Miller, A. Naeter, B. L. Robertson, Charles F. Scott, G. H. Sechrist, E. M. Strong, G. W. Swenson, R. G. Warner, and ex officio all Student Branch counselors.

Table I. Section Membership and Meetings During Year Ending April 30, 1942

Section	AIEE Members August 1940	AIEE Members August 1941	Number of Meetings	Average Attendance	Average Attendance as Per Cent of Membership, August 1941
Akron	85	87	9	49	56
Alabama	40	45	4	29	64
Arizona		28	9	31	111
Boston	415	439	8	180	41
Central Indiana	132	144	6	45	31
Chicago	769	810	11	202	25
Power group			4	55	
Industrial group			3	60	
Cincinnati	207	212	9	89	42
Cleveland	332	363	9	203	56
Technical group			6	62	
Columbus	92	86	10	54	63
Connecticut	279	299	7	337	113
Denver	192	177	8	92	52
East Tennessee	132	137	7	47	34
Erie	57	56	4	56	100
Florida	78	100	3	285	285
Fort Wayne	109	80	8	54	68
Georgia	109	122	5	125	102
Houston	141	141	8	107	76
Iowa	72	81	7	63	78
Ithaca	51	55	5	38	69
Kansas City	148	128	8	44	34
Lehigh Valley	191	194	8	100	52
Los Angeles	506	507	11	164	32
Louisville	61	86	7	87	101
Lynn	147	167	9	550	329
Technical meetings			2	63	
Local conventions			2	150	
Madison	67	72	9	118	164
Rock River Valley subsection			8	84	
Mansfield	68	77	8	69	90
Maryland	252	286	9	100	35
Memphis	72	66	10	36	54
Mexico	56	57	5	55	96
Michigan	376	398	9	135	34
Milwaukee	280	286	10	101	35
Minnesota	107	103	11	47	46
Montana	36	40	8	131	330
Muscle Shoals	33	30	10	34	113
Nebraska	48	45	4	37	82
New Mexico–West Texas	47	43	7	27	63
New Orleans	123	132	10	61	46
New York	3,346	3,492	4	475	14
Power group			5	191	
Communication group			3	325	
Illumination group			3	267	
Basic science group			3	160	
Transportation group			3	115	
Niagara Frontier	197	207	10	100	48
North Carolina	91	101	3	80	79
North Texas	170	181	10	89	49
Oklahoma City	117	117	9	93	80
Philadelphia	650	750	8	165	22
Pittsburgh	564	630	8	187	30
Pittsfield	192	197	5	123	62
Popular meetings			9	1,189	
Portland	176	196	13	89	45
Transmission and distribution committee			1	46	
Communication committee			2	27	
Providence	99	95	10	74	78
Rochester	103	110	7	127	115
St. Louis	273	304	10	96	32
San Diego	36	46	9	27	59
San Francisco	507	543	12	90	17
Saskatchewan	15	10	6	30	300
Schenectady	434	484	11	238	49
Technical discussion meetings			3	65	
Seattle	164	202	8	105	52
Sharon	105	120	9	119	99
South Bend		52	9	131	251
South Carolina	37	50	1		
South Texas	45	48	9	68	142
Spokane	81	84	8	54	64
Springfield	57	51	9	66	129
Syracuse	75	103	11	86	83
Toledo	71	80	11	61	76
Toronto	342	363	16	168	46
Tulsa	94	90	10	74	82
Urbana	76	68	5	398	585
Utah	76	72	8	243	337
Idaho technical committee			5	28	
Vancouver	97	91	10	45	49
Virginia	111	137	2	32	23
Washington	370	491	11	170	35
Technical sessions committee			3	114	
West Virginia	48	54	2	25	46
Wichita	52	64	10	41	64
Worcester	63	65	8	67	103
Total—72 Sections	14,844	15,927	647		

Total attendance, 78,254

Table II. Branch Meetings Held

Branch	Number	Average Attendance
Akron, University of	2	11
Alabama, Polytechnic Institute	4	37
Alabama, University of	6	16
Alberta, University of	9	24
Arizona, University of	14	13
Arkansas, University of	3	18
British Columbia, University of	12	21
Brooklyn, Polytechnic Institute of		
Day division	6	47
Evening division	6	22
Brown University		
Bucknell University	8	15
California, Institute of Technology	6	49
California, University of	15	37
Carnegie Institute of Technology	27	70
Case School of Applied Science	4	25
Catholic University of America	2	12
Cincinnati, University of	7	56
Clarkson College of Technology	6	31
Clemson Agricultural College	7	31
Colorado State College	6	20
Colorado, University of	8	124
Columbia University	4	22
Connecticut, University of	8	78
Cooper Union		
Day division	7	25
Evening division	8	20
Cornell University	5	58
Delaware, University of*		
Denver, University of	4	31
Detroit, University of	6	26
Drexel Institute of Technology	5	14
Duke University	13	23
Florida, University of	10	22
George Washington University	7	32
Georgia School of Technology	4	63
Harvard University	3	14
Idaho, University of	15	28
Illinois Institute of Technology	12	60
Illinois, University of	4	115
Iowa State College	10	64
Iowa, University of	21	40
Johns Hopkins University	10	22
Kansas State College	11	85
Kansas, University of	9	39
Kentucky, University of	9	61
Lafayette College	4	17
Lehigh University	5	193
Louisiana State University	11	26
Louisville, University of	5	87
Maine, University of	10	18
Manhattan College	8	35
Marquette University	3	16
Maryland, University of	9	89
Massachusetts Institute of Technology	4	33
Michigan College of Mining and Technology	12	36
Michigan State College	6	50
Michigan, University of	9	36
Milwaukee School of Engineering	5	48
Minnesota, University of	6	44
Mississippi State College	5	24
Missouri School of Mines and Metallurgy	4	50
Missouri, University of	6	36
Montana State College	22	23

*Authorized by board of directors, January 29, 1942.

Annual Report for 1941–42

All Sections were active, and only 7 held fewer than 5 meetings each, while 28 Sections held 10 or more meetings each.

A large majority of the Sections devoted portions of their programs for the year to various phases of defense and war activities.

A new Student Branch organized at the University of Delaware brought the total number to 124.

During Year Ending April 30, 1942

Branch	Number	Average Attendance
Nebraska, University of	13	38
Nevada, University of	2	24
Newark College of Engineering	6	29
New Hampshire, University of	8	23
New Mexico State College	12	9
New Mexico, University of	5	16
New York, College of the City of		
Day division	22	60
Evening division	8	13
New York University		
Day division	6	17
Evening division	7	21
North Carolina State College	11	40
North Dakota Agricultural College	8	17
North Dakota, University of	9	12
Northeastern University	12	46
Northwestern University	3	36
Norwich University	3	25
Notre Dame, University of	5	39
Ohio Northern University	12	36
Ohio State University	6	36
Ohio University	4	39
Oklahoma A. & M. College	10	88
Oklahoma, University of	9	36
Oregon State College	12	45
Pennsylvania State College	4	68
Pennsylvania, University of	2	18
Pittsburgh, University of	4	82
Puerto Rico, University of		
Pratt Institute	9	36
Princeton University	1	20
Purdue University	7	131
Rensselaer Polytechnic Institute	1	108
Rhode Island State College	4	15
Rice Institute	14	37
Rose Polytechnic Institute	8	34
Rutgers University	5	22
Santa Clara, University of	9	16
South Carolina, University of	1	14
South Dakota State College	13	26
South Dakota State School of Mines	5	17
Southern California, University of	8	22
Southern Methodist University	6	28
Stanford University	9	43
Stevens Institute of Technology		
Swarthmore College	1	27
Syracuse University	1	8
Tennessee, University of	8	26
Texas A. & M. College	1	42
Texas Technological College	11	86
Texas, University of	16	64
Tufts College	5	32
Tulane University	5	33
Union College	2	58
Utah, University of		
Vermont, University of	9	17
Villanova College	8	10
Virginia Military Institute	5	64
Virginia Polytechnic Institute	24	48
Virginia, University of	2	18
Washington, State College of	14	43
Washington, University of	9	57
Washington University	5	24
West Virginia University	10	25
Wisconsin, University of	11	106
Worcester Polytechnic Institute	4	50
Wyoming, University of	9	11
Yale University	6	14
Total—124 Branches		

Table III. Section Meetings Held During Last Three Fiscal Years

	Fiscal Year Ending April 30		
	1940	1941	1942
Number of Sections	70	72	72
Number of meetings held	701	703	647
Average number of meetings	10.0	9.8	9.0
Total attendance	91,949	92,554	78,254
Average attendance per meeting	131	132	121

Table IV. Branch Meetings Held During Last Three Fiscal Years

	For Year Ending April 30		
	1940	1941	1942
Number of Branches	121	123	124
Number of meetings held	1,346	1,163	946
Average number of meetings	11.1	9.5	7.6
Total attendance	64,972	52,285	37,785
Average attendance per meeting	48.3	45.0	39.9

Table V. Conferences on Student Activities

District	Location	Date
5	Illinois Institute of Technology, Chicago, Ill.	5/19/41
8 and 9, University of British Columbia	Yellowstone National Park (Pacific Coast convention)	8/27–29/41
2	University of Maryland, College Park, Md.	10/24–25/41
7	St. Louis, Mo. (South West District meeting)	10/8–10/41
4	New Orleans, La. (Southern District meeting)	12/3–5/41
6	Colorado State College, Fort Collins, Colo.	4/17–18/42
1	Schenectady, N. Y. (North Eastern District meeting)	4/29–5/1/42

Table VI. Section or Joint Section and Branch Meetings With Active Student Participation

Sections	Branches	Date	Student Talks	Attendance
New Orleans	Tulane University	4/18/41	2	48
Chicago	Illinois Institute of Technology Northwestern University	5/1/41	3	100
Iowa	Iowa State College University of Iowa	5/1/41	6	74
Cincinnati	University of Cincinnati	5/8/41	6	115
Spokane	University of Idaho State College of Washington	5/9/41	2	109
Virginia	Virginia Polytechnic Institute	5/10/41	4	10
Tulsa	University of Arkansas Oklahoma A. & M. College	5/12/41	4	54
Portland	Oregon State College	5/17/41	2	80
North Carolina	North Carolina State College	11/7/41	1	
Oklahoma City	University of Oklahoma	5/21/41	2	89
St. Louis	University of Missouri Missouri School of M. & M.	5/23/41	5	66
Worcester	Worcester Polytechnic Inst.	5/23/41	5	50
Utah	University of Utah	5/26/41	4	49
Toronto	University of Toronto	1/23/42	4	88
Vancouver	Univ. of British Columbia	3/3/42	4	63
New Orleans	Louisiana State University	3/20/42	4	77
Houston	Rice Institute Texas A. & M. College	3/27/42	4	80
Cincinnati	University of Cincinnati	4/2/42	4	55
Akron	University of Akron	4/14/42		
Los Angeles	California Inst. of Tech. University of Southern Calif.	4/14/42	5	170
Cleveland	Case School of Applied Science	4/16/42		
Louisville	University of Louisville	4/17/42	1	30
South Bend	University of Notre Dame	4/22/42	2	45
Iowa	Iowa State College University of Iowa	4/23/42	7	41
Minnesota	University of Minnesota	4/23/42	3	50
New Orleans	Tulane University	4/24/42	2	
San Francisco	University of California University of Santa Clara Stanford University	4/24/42	3	88
Spokane	University of Idaho State College of Washington	4/24/42	3	85
Worcester	Worcester Polytechnic Inst.	4/24/42	2	33
Totals—23 Sections, 33 Branches, 29 meetings			94	1,749

ABSTRACTS • • •

TECHNICAL PAPERS previewed in this section will be presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and are expected to be ready for distribution in advance pamphlet form within the current month. Copies may be obtained by mail from the AIEE order department, 33 West 39th Street, New York, N. Y., at prices indicated with the abstract; or at five cents less per copy if purchased at AIEE headquarters or at the convention registration desk.

Mail orders will be filled
AS PAMPHLETS BECOME AVAILABLE

Basic Sciences

42-128—The Effect of Initial Conditions on Subharmonic Currents in a Nonlinear Series Circuit; *Stephen J. Angello (application pending). 15 cents.* In a series circuit consisting of a resistance, capacitance, and a saturable inductor with a sinusoidal impressed electromotive force, it is possible to establish curents with a frequency which is a submultiple of the frequency of the applied voltage. A device was constructed for closing a switch in the circuit at any point on the input voltage wave. The wave form of the current in the circuit for the first four cycles after closure could be viewed. Investigation of the current response for initial-voltage phase angles ranging from 0 to 360 degrees showed that:

(*a*) Phase angles of 0 and 90 degrees usually yield 60-cycle current.

(*b*) Phase angles of 90 and 270 degrees yield subharmonic current for certain impressed voltages, and a circuit resistance less than seven ohms.

(*c*) The wave form of the first four cycles of the current is dependent on the initial flux linkages in the inductor core.

42-141—Energy Flow in Electric Systems—the Vi Energy-Flow Postulate; *Joseph Slepian (F '27). 20 cents.* The conditions which a valid postulated electric-energy flow must satisfy are given and are stated to be insufficient for its unique determination. The commonly used *Vi* energy-flow postulate is shown by examples to be not generally valid, but by adding a simple term it can be made equally valid with other valid energy-flow postulates. Various examples are given of the application of this corrected energy-flow postulate. On power systems, the engineer commonly limits his use of the uncorrected *Vi* postulate to applications where the correcting term should have a negligible net effect. Various examples of such use are discussed.

42-142—Formulas for Calculating Short-Circuit Stresses in Bus Supports for Rectangular Tubular Conductors; *T. J. Higgins (A '40). 15 cents.* Formulas are derived by which can be calculated the values of the "shape correction factor" k, essential to the computation of short-circuit stresses in bus supports for rectangular tubular conductors. Differentiation of the known formula for the magnetic-field energy of a circuit comprised of two long parallel rectangular tubular conductors, expansion of the resulting derivative in an infinite series, and appropriate selection therefrom of the terms of consequence yield formulas, expressed as rapidly converging series, for k. These formulas enable one to calculate rapidly the short-circuit stresses in the supports of single or polyphase a-c or d-c busses comprised of identical strap or rectangular tubular conductors with parallel coplanar axes. Several detailed numerical examples illustrate application of these formulas to busses comprised of strap conductors or of square tubular conductors.

42-140—On Eddy Currents in a Rotating Disk; *W. R. Smythe. 15 cents.* There seem to be no simple formulas for calculating the braking torque on conducting disks rotating in magnetic fields. This paper derives briefly a general formula given by Maxwell in 1873 and from this develops equations giving the torque when a uniform magnetic field is produced over one or two circular areas by a permanent magnet. These equations are good approximations when $2\pi\omega b\gamma<0.1$ where ω is the angular velocity in radians per sec, b the thickness in cms and γ the conductivity of the disk material in electromagnetic units. Maps of the eddy currents are drawn. The torque for n poles can be found accurately by the method used here for two and is roughly equal to n times that for one pole. The demagnetizing action of the eddy currents on an electromagnet is calculated and a simple approximate formula for the torque as a function of speed is derived. A numerical example is worked out, using dimensions larger than those postulated for accuracy in the derivation but giving results that agree quite well with the experimental data of Lentz for a brake of comparable size. This would indicate that these formulas can be used for a rough guide in designing large brakes.

Electrical Machinery

42-115—Emergency Overloading of Air-Cooled Oil-Immersed Power Transformers by Hot-Spot Temperatures; *V. M. Montsinger (F '29), P. M. Ketchum (A '39). 30 cents.* During the period of the war, the need for obtaining maximum transformer-overload capacity is very great, particularly under rare emergency conditions. Since the amount of overload depends upon maximum rather than average temperatures, hottest-spot temperatures and oil gradients were measured in several transformers of different ratings under (1) rated load, (2) moderate ultimate overloads, and (3) short-time heavy overload conditions. The results of the tests are analyzed with the conclusions (1) that the hottest-spot rise over average rise at rated load ranges from five to ten degrees centigrade for most power transformers, and (2) that the hottest-spot rise over top oil varies approximately as $\text{loss}^{0.8}$ for both ultimate and short-time overloads. The paper suggests increasing the present American Standards Association temperature limits and shows how the proposed temperature limits affect the life of insulation judged by laboratory aging tests reported in another paper to be presented at the same session. Calculated short-time overloads are given to show how they vary with different characteristics of transformers.

42-132—Precision Speed Control for World's Largest Induction Motor; *R. R. Longwell, M. E. Reagan (F '41). 15 cents.* Automatic control equipment has been developed for all types of machines and apparatus but that for a 40,000-horsepower motor is the most unusual single installation. Following the single manual operation of energizing the master relay of the automatic control apparatus, two 5,000-horsepower motor-generator sets are started and brought up to speed in sequence and the 40,000-horsepower motor is connected to the substation bus automatically. This is accomplished without requiring more than 2,000-kw increase in power demand from the a-c system in any 60-second interval. The motor is started and brought up to speed merely by the simple operation of setting the pointer of a control dial at the speed desired. Speed is maintained within 0.3 to 0.5 per cent of the speed setting at any point in the speed range of 37.5 to 295 rpm.

Electronics

42-122—Electronics of the Fluorescent Lamp; *Mark A. Townsend (A '42). 20 cents.* The fluorescent lamp can be considered as two energy converters connected in series. The first of these is a gaseous conductor which converts electric energy into ultraviolet radiant energy. The second is a coating of fluorescent powder which acts as a frequency changer to convert ultraviolet radiation into visible light. The gaseous conductor, or gaseous-conduction lamp, is an electronic device and the electronics of the fluorescent lamp are concerned with the operation of this part of the lamp. The performance of fluorescent powders is mentioned briefly in order to illustrate the requirements which the gaseous conductor must meet. A major part of the paper is devoted to a description of the fundamental mechanisms of current conduction and the relation of these processes to the production of radiation and to the over-all electrical characteristics of the lamp.

Industrial Power Applications

42-118—Ignitron Rectifiers in Industry; *J. H. Cox (A '25), G. F. Jones. 20 cents.* When a new device is developed, for various reasons it is usually adopted and proved by a particular industry. In the case of the ignitron rectifier, the first applications were in transportation service in mines and railways. The apparatus and its performance in these early installations was discussed by the present authors in an earlier Institute paper. Since that time, and as superior operating results have become known, the ignitron has been adopted by other industries, most notably the electrochemical industry. In excess of 2,000,000 kw of ignitron rectifier units has been purchased by that industry alone. This paper discusses primarily installations for large power concentrations.

42-138—Electric Equipment for Large Electrochemical Installations; *T. R. Rhea (A'41), H. H. Zielinski (M'35). 30 cents.* The increased demand for aluminum, magnesium, chlorine, copper, and zinc for war purposes has made these electrolytic processes the largest consumers of electric energy in this country. The most frequently encountered direct current and voltage requirements of these four principal electrolytic processes are discussed. An illustrative current-time and voltage-time characteristic curve for starting a chlorine cell line is shown and the requirements that such characteristics impose on the electrical equipment are discussed. The current-voltage characteristics of aluminum, magnesium, chlorine, and zinc cell lines are also shown. A typical installation of conversion equipment for an electrolytic-process plant is given and the reasons for selecting the particular types of electric equipment, and its physical and electrical arrangement are discussed. As part of this discussion, there are included characteristic curves of rectifiers showing the effect of ignition control on power factor for 6-, 12-, and 36-phase combinations. A table gives a "rule of thumb" relationship between the number of phases and kilowatt limits which have been found in practice to provide operation reasonably free from telephone interference. A tabulation of phase-shifter combinations is also given, by means of which multiphase operation can be obtained with various combinations of standard 6-phase rectifier transformers.

42-143—Electrical Features of Design and Operation of the Plantation Pipe Line; *M. A. Hyde (A'27), H. B. Britten. 20 cents.* This 1,260-mile line, utilizing 30,000 horsepower in motors, is the largest refined-products line ever built and is unique in the number of different products handled and the number of delivery points. The design of this line embodies the solution of unusual engineering problems as to flexibility for present schedules and possible future expansion, the safe handling of hazardous petroleum products, the coordination of electric apparatus to pump requirements, and the development of a system of control.

42-139—A New Multipole High-Speed Air Circuit Breaker for Mercury-Arc-Rectifier Anode Circuits and Its Relation to the Arc-Back Problem; *J. W. Seaman (A'32), L. W. Morton (A'38). 30 cents.* One persistent factor in connection with the application of rectifiers which has claimed much attention is the phenomenon of arc-back. Various methods of attack have been employed successfully. High-speed anode switching appears to be the most satisfactory way to handle this problem. Such a solution is widely used in the aluminum industry. Part I of this paper includes an analysis of the arc-back problem and various means of protection from its effects. Reasons why high-speed anode switching is an improved type of arc-back protection are set forth. Part II of this paper describes a multipole high-speed air circuit breaker. The requirements which the breaker must meet are discussed and the electrical and mechanical features described. Performance of the breaker was checked in field tests. Oscillographic data are presented to prove that the performance is acceptable.

Instruments and Measurements

42-108—Modern Cathode-Ray Oscillograph for Testing Lightning Arresters; *E. J. Wade (A'23), T. J. Carpenter (A'38), D. D. MacCarthy (A'28). 15 cents.* This paper describes the design features and performance of a high-voltage cathode-ray oscillograph of the cold-cathode type with the film in the evacuated chamber. This instrument was designed to meet the exacting needs of a laboratory devoted to lightning-arrester development and the study of insulation protection. The important requirements for this work include: accuracy of measurement, convenience of operation, consistency of timing and switching, and high writing speed essential in studying fast nonrecurrent transients. Elements that enable these requirements to be fulfilled are described, and illustrative oscillograms included. Sweeping speeds as fast as 44 centimeters per microsecond and writing speed in excess of 3,000 centimeters per microsecond enable this oscillograph to record ultrafast transients. The instrument has been of great value during two years of constant use.

42-110—Theoretical Possibilities in an Internally Heated Bimetal Type of Thermal Watt-Demand Meter; *Edward Lynch (M'35). 25 cents.* An elaboration on and greater dissemination of knowledge about thermal demand meters leads to a mathematical expression of "logarithmic average" which can be applied to varying as well as uniform loads. Such an expression defines a quantity which a theoretical thermal demand meter would indicate. Commercial meters do not exactly indicate this quantity under all load conditions because of "diffusion" but an analysis of the factors entering into diffusion indicates that an "internally heated" bimetal meter will greatly reduce this effect since the heat is generated in a distributed manner. Experimental models not only substantiate this theory but indicate as well that high torques are available in such meters which can be utilized in sustaining greater accuracies.

42-123—A New Moving Magnet Instrument for Direct Current; *H. T. Faus (M'34), J. R. MacIntyre (A'41). 15 cents.* A d-c instrument recently developed for use as a battery-testing voltmeter has characteristics which make it an acceptable substitute for a permanent-magnet moving-coil instrument of normal sensitivity. The element includes a diametrically magnetized rotor, acted upon by two magnetic fields. These fields are displaced by 135 angular degrees, and are produced by two control magnets and a current-carrying coil. The field of the control magnets provides a restoring torque, and the rotor assumes the position at which its poles are in line with the resultant of the control magnet field and the coil field. The desired characteristics have been obtained by the use of permanent-magnet materials new to the instrument art. The entire element is enclosed in a metal cup approximately three-fourths-inch in diameter by one-half-inch in length. This small size renders it suitable for many applications in which there would not be room for a moving-coil-instrument element.

42-134—A New Jewel for Indicating Instruments; *F. K. McCune (A'33), J. H. Goss (A'35). 20 cents.* The problem involved in obtaining jewels for electrical indicating instruments and equivalent apparatus is discussed. Test data showing the performance of materials that might be substituted for sapphire are presented, and in particular, the data show that jewels properly made from one of these materials are very acceptable for the purpose.

Land Transportation

42-121—Sleet Problems on Electrified Railroads; *H. F. Brown (M'25). 20 cents.* Sleet storms, or more properly ice storms, have always presented serious difficulties to the operators of overhead electric conductors of all classes. For more than 25 years some electric power companies have been using the circulation of electric current to heat the conductors, either to melt off the ice or to prevent it from forming. The technique of such procedure is well known, and will not be described beyond the presentation of fundamental theoretical data. Electrified railroads operating with overhead contact systems have similar ice-storm problems, and in addition, a number of special problems caused by such ice. These problems are outlined, and some means that have been used or proposed for their solution are described. Operating experience with one special application to a case of severe exposure to ice storms and high wind velocities is cited. Some of these special railroad problems have not yet been satisfactorily solved.

42-126—Improvements in Preventive Coil Control for A-C Locomotives With Particular Reference to "Resistor Transition"; *P. H. Hatch (M'29), H. S. Ogden (M'36). 20 cents.* A-c locomotive acceleration control has undergone a more or less steady improvement in its design features. The paper deals with some of the more important improvements in the control that are now in general use, and also covers an interesting application of a shunting resistor the function of which is to prevent saturation of a preventive coil undergoing transition. In discussing the resistor transition the results of the original experiments are covered in some detail and graphic illustrations are given showing the improvement in the notching on a locomotive that is obtained when the scheme is applied. Complete elimination of the sag back between notches is achieved and the high peak voltages caused by the saturation of the preventive coil completely vanish. Service experience with locomotives so equipped indicates much quieter operation of the tap switches, smoother locomotive acceleration, less tendency of the locomotive drivers to slip while accelerating, and improved life of the tap-switch arc-chute sides and contact tips. A scheme of control for accomplishing the desired sequence of operation of the tap switches and resistor contactor that is simple, yet quite effective and reliable, is described in detail.

42-127—Electric Control for Steam Boilers on Diesel and Straight Electric Locomotives; *E. H. Burgess. 15 cents.* Electric control is a necessity in the operation of the modern steam boiler if it is expected to have maximum efficiency, safety in operation, and constant operation at the various outputs required. Installation and operating conditions vary over a wide range; therefore, to meet these conditions, the controls used differ in type and quantity to meet those actually needed for good operation, as well as those required to comply with rules and regulations under which the installations are permitted. Generally, the method of operation of such boilers can be classified in two categories, automatic and semiautomatic. Electric control is further separated into two component parts: operating necessities required for actual boiler operation, and safety features required under legal regulations, including those needed for the safety of the equipment itself in case of failure of any major part. Operating conditions of today require a trouble-free full-capacity output boiler that can be operated without the constant need of an attendant. To obtain this service, electric controls of approved types are a vital part of the complete boiler assembly and should not be overlooked in the design of equipment for the most satisfactory operation.

42-137—Electrical Facilities and Operating Plan for the First Chicago Subway; *Charles E. DeLeuw. 25 cents.* The $64,000,000 five-mile subway project now being completed will provide long-needed rapid-transit terminal facilities in the Chicago downtown district. The subways were built with track sections in tunnel—at low level. Platforms 500 feet long are provided at stations except in the congested area where there is a continuous island platform 3,300 feet in length with access provided by mezzanine stations in each block. Power will be 600 volts d-c. The 144-pound contact rail will be energized by existing substations and energy delivered by conventional positive and negative feeders. Adequate sectionalization of the entire system is provided largely by automatic operation. Centralized supervisory control of the all-relay type includes most modern accessories. Train movements will be controlled throughout by a modern signal and electropneumatic interlocking system with automatic stops. Signals are spaced and timed for operation of 40 trains per hour on each track. Signals and other electrical facilities, including lighting, pumps, fans and escalators, will have a-c power supply. Fluorescent lamps will be utilized at all subway stations. Lighting intensities on mezzanine floors and station platforms will vary from six to eight foot-candles. Escalators are provided at all stations. At the downtown and heavier outside stations two 4-foot escalators are provided, operating at a speed of 90 lineal feet per minute.

Power Generation

42-113—Frequency Control of Load Swings; *J. E. McCormack (M'37), R. J. Lombard. 15 cents.* This paper describes a new method of dispatching generator load changes on the Consolidated Edison Company system. This method was adopted after extensive tests had been made to determine the cause and extent of load swings on tie feeders and generating stations. Manual adjustments of generator load are now made only when such adjustments tend to restore frequency to normal. The magnitude and occurrence of load swings have been so reduced that they no longer present a problem.

Power Transmission and Distribution

42-107—Study of Driven Rods and Counterpoise Wires in High Resistance Soil on Consumers Power Company 140 Kv System; *J. G. Hemstreet (A'21), W. W. Lewis (F'38), C. M. Foust (M'31). 25* cents. A test setup was made in high-resistance soil in western Michigan, by which the effectiveness of deep-driven rods and parallel and radial counterpoise wires could be evaluated. It was found that counterpoise wires were very effective compared with normal tower footings in picking up lightning current, and that the two types of counterpoise were equally effective. Deep-driven rods (50 to 150 feet) in this type of soil are much more efficient than counterpoise wires. Another setup allowed a measurement of the voltage drop between tower and a rod 6 feet away from the tower. From these measurements it was found that the ratio of surge resistance to normal tower-footing resistance decreases with increasing currents and increasing resistance, and may be as low as 0.04 for soils where the normal resistance ranges up to 1,000 ohms or more and tower currents up to approximately 50,000 amperes.

42-109—Abnormal Currents in Distribution Transformers Due to Lightning; *J. M. Bryant (M'13) and M. Newman (A'39). 15 cents.* Abnormal currents in distribution transformers caused by lightning are analyzed both theoretically and from field experiences. Under certain conditions of direct stroke transferred through the arrester to the neutral of the secondary winding, excessive currents may wreck the secondary windings as a result of electromagnetic forces. Long-duration surges, or shorter repeated surges, saturate the transformer cores producing greatly increased surge currents in the primary windings which influence fuse failures. The saturation of the cores by unidirectional lightning surges also bring about increased power-frequency magnetizing currents influencing sectionalizing-fuse and circuit-breaker operations.

42-111—Load Ratings of Cable—II; *Herman Halperin (M'26). 30 cents.* The loads on many existing underground paper-insulated cables may be safely increased during at least this war period. This is substantiated particularly by the presentation of data showing that for many cables the allowable copper temperatures during occasional emergencies may be from 100 to 140 degrees centigrade, or much above the maximum temperatures given in the specifications originally used in purchasing the cable. In determining the value for any given case, consideration must be given to a large number of factors. One important factor is cracking of lead sheaths as a result of reciprocating daily cable movement in the manholes. Test and field data are analyzed to indicate the life of sheaths under various conditions. Some data are given on the effect of heat on various cables, on the thermal characteristics of cables, and on matters relating to heating of conduits.

42-112—Series Capacitors for Transmission Circuits; *E. C. Starr (F'41), R. D. Evans (F'40). 30 cents.* This paper presents the results of an investigation concerning the use of series capacitors on long transmission lines. The various application problems are discussed. Results of stability and hunting tests on miniature systems equipped with series capacitors are included.

42-117—Stability Study of A-C Power-Transmission Systems—I and II; *John M. Grzybowski Holm (M'29). 30 cents.* The principles of design are given of 31 transmission systems selected for stability studies on the a-c calculating board. The systems transmit 50,000 to 800,000 kw (at the receiving end) over distances of 50 to 500 miles at 69- to 345-kv sending-end voltages. Complete system data are given, including the reactances of generators, transformers at both ends of the transmission line, and the load. The kilovolt-ampere capacity of receiving-end condensers is given for all systems as measured on the analyzer. Steady-state and transient-stability limits of systems are given as obtained on the network analyzer. No special devices for stability improvements are used, except that the 250- and 500-mile lines are equipped with intermediate synchronous condensers.

Curves are plotted permitting determination of the system stability limit, or the voltage required for a certain condition. Curves are suggested for rapid estimating of the steady-state and transient-stability limits of systems. Means necessary to increase the stability limits of systems are studied on the a-c network analyzer. The number, location, and size of the intermediate condenser station is investigated. Its effect on the line operating conditions and on the stability limits is discussed. Curves are given showing at various fault-clearing times the increase in the transient-stability limit of 50- to 500-mile lines due to grounding the sending-transformer neutral through a 13-per-cent resistance. A study is made of the effect of the reactor on the generator bus on the increase of the steady-state and transient-stability limits. Conditions for its application are discussed, and curves suggested for estimating the improvement obtained in the transient-stability limit, as well as the required size of the reactor on the bus. The methods to be used on the systems designed are given, in order that their stability limits may be brought up to the ratings desired.

42-133—120-Kv Compression-Type Cable; *I. T. Faucett* (*F'28*), *L. T. Komives* (*A'41*), *H. W. Collins* (*A'22*), *R. W. Atkinson* (*F'28*). *20 cents*. Armored compression cable, in which temperature compensation is obtained by diaphragm action of the lead sheath subjected to external gas pressure, has been in operation in Europe for a number of years at voltages up to 120 kv. Because of the difficulties of obtaining precise information on all phases of the foreign installations and the uncertainty involved in extrapolating foreign experience to the relatively large loads transmitted in this country, laboratory tests and an experimental field installation were made to recheck fully the mechanical, electrical, and thermal features of this system. This investigation showed that the increase in conductor size and temperature range did not influence unfavorably the operation of such cable and that this type of system is well suited for loads of 100,000 kva or more. It was also found that the external armor heretofore used on compression cable can be eliminated, together with the expansion bends in the pipe line on each side of the joints.

42-135—120-Kv High-Pressure Gas-Filled Cable; *I. T. Faucett* (*F'28*), *L. I. Komives* (*A'41*), *H. W. Collins* (*A'22*), *R. W. Atkinson* (*F'28*). *25 cents*. This paper deals with the theory, manufacture, and testing of 120-kv high-pressure gas-filled cable and describes an experimental installation by the Detroit Edison Company in co-operation with the General Cable Corporation and a subsequent commercial installation on the Detroit Edison system. While high-pressure gas-filled cables, encased in reinforced lead sheaths, have been in successful operation abroad, these are the first experimental and first commercial installations in the world of this type of cable in steel pipe. The experimental installation showed that nonleaded high-pressure gas-filled cable could be designed and manufactured to operate with equal success in steel pipe, and that it could be installed without the insulation being adversely affected by atmospheric exposure or mechanical handling. The commercial installation, consisting of a seven-mile circuit of 120 kv cable capable of transmitting 95,000 kva continuously, was put into service on December 31, 1941.

Protective Devices

42-114—A Compressed-Air Operating Mechanism for Oil Circuit Breakers; *R. C. Cunningham, A. W. Hill* (*M'41*). *15 cents*. Compressed air is used as a source of power to close oil circuit breakers and has the advantage of being immediately available in a charged reservoir. Heavy closing currents are avoided, and faster breaker speeds are obtained, especially in reclosing. The essential features—an air supply, operating cylinder, and control means—are combined in one housing to provide an independent unit, with pull characteristics adjusted to meet the load requirements of conventional breakers, as demonstrated by test oscillograms.

42-116—Current and Potential Transformer Standardization; *J. E. Clem* (*F'38*), *J. H. Neher* (*M'38*), *E. L. Harder* (*M'41*). *30 cents*. This report has been written to discuss the considerations that led to the adoption of the material in the revision of Section IV, Instrument Transformers, of the Proposed ASA Standard for Transformers, *C*-57. Primary current ratings of current transformers are discussed and the required list materially reduced in number. Standard burdens for rating purposes for current transformers are discussed and a single list covering requirements for relaying and metering applications proposed. A method of specifying the accuracy classification of current transformers is proposed on the basis of the total error introduced into the circuit by the transformer and establishes narrower limits than before. A new section on standard accuracy classes for current transformers for relay service has been added to the Standard and this section is discussed and explained. Preferred ratios for potential transformers, together with the corresponding insulation levels, are reviewed. A simple method of calculating current transformer performance is presented for transformers having negligible internal regulation.

42-119—Tests and Analysis of Circuit-Breaker Performance When Switching Large Capacitor Banks; *T. W. Schroeder* (*A '37*), *E. W. Boehne* (*M '37*), *J. W. Butler* (*M '39*). *30 cents*. At the present time the use of capacitors in banks up to 10,000 kva is being given increased consideration and several installations having steps in this range have already been made. With this growing use of capacitors, it is becoming increasingly necessary to apply adequate power circuit breakers to the task of switching these large banks. Large transient equalizing currents may obtain when energizing capacitors. It has long been recognized that the job of switching capacitive circuits, as defined particularly by long unloaded transmission lines, can become difficult, so it logically follows that both the duty on the breaker and its effect on the system during switching of large capacitor banks should be investigated. This paper offers an analysis of the problem together with test results on full scale and miniature capacitor banks. Certain conclusions are drawn relative to the duty on and selection of breakers for this service.

42-120—Transient Recovery Voltages and Circuit-Breaker Performance; *R. C. Van Sickle* (*M'37*). *30 cents*. Recent studies of the transient recovery voltages which electric-power systems can impress on high-voltage circuit breakers during the clearing of faults resulted in experimental investigations of four types of circuit breakers to determine their performance when subjected to these conditions. The tests demonstrated the response of the breakers to both single- and double-frequency transient recovery voltages having natural frequencies up to 200,000 cycles per second. They showed similarities in the performances of the different types of breakers and demonstrated that the difficulty of interruption did not increase indefinitely with increasing natural frequency and that the maximum arcing time at a given voltage and current was obtainable in a high-power circuit-breaker testing laboratory.

42-125—A New Single-Phase-to-Ground Fault-Detecting Relay; *W. K. Sonnemann* (*A'38*). *15 cents*. In the application of differential relays, need has frequently arisen for a supervising relay that will detect the existence of a single-phase-to-ground fault condition to the exclusion of all others. Heretofore, the only scheme available has been to utilize a relay energized by zero-sequence quantities. Generally, the relay has been energized by a current transformer connected in the station ground. Such a relay, however, will also detect the existence of a two-phase-to-ground fault and only partially solves the problem. The new relay described in this paper derives its operating force from the zero-sequence voltage at the bus, and is restrained by the negative-sequence voltage at the bus. The addition of properly proportioned negative-sequence restraint provides the relay with a means of recognizing a single-phase-to-ground fault only. It is applicable on those systems where the zero-sequence impedance of the system exceeds the negative-sequence impedance by a reasonable margin.

42-129—Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits; *G. F. Lincks* (*A'37*), *C. R. Craig*. *30 cents*. The expense for service restoration is of real importance in studying the relative value of different types of overcurrent protection for distribution circuits. The effect of such protection on the continuity of electric supply has been studied mathematically. Using the identical assumptions and calculations this study has been extended to show the effect of all different types of overcurrent protective equipment and combinations thereof on the man-hours and the automotive miles required to locate faults, to make necessary repairs, and to restore service (restoration expense). The study covers a range of temporary faults from 25 to 85 per cent of the total number of faults. This additional knowledge of the relative restoration expense of distribution overcurrent protective equipment and its applications, combined with that made available previously on the relative benefits in service continuity, should be of value in economical system planning.

42-130—Transient Recovery-Voltage Characteristics of Electric-Power Systems; *H. P. St. Clair (M '29), J. A. Adams (A '24). 15 cents.* While it has been known for some time that transient recovery-voltage conditions affect the design and operation of circuit breakers, there has not been available a comprehensive picture of the severity of these conditions throughout the electric-power industry. This paper presents outstanding results and conclusions from a recent survey carried out under the sponsorship of the committee on electric switching and switchgear of the Association of Edison Illuminating Companies and made available by permission of that organization and of the participating electric-power companies to the AIEE committee on protective devices for such presentation. The results, showing transient recovery-voltage conditions of greater severity and more widespread occurrence than previously suspected, are of considerable interest to circuit-breaker users, and are of major importance to the circuit-breaker manufacturers in the development of new designs.

42-131—Practical Calculation of Circuit Transient Recovery Voltages; *J. A. Adams (A '24), W. F. Skeats (M '36), R. C. Van Sickle (M '37), T. G. A. Sillers (A '26). 30 cents.* A practical method is offered for calculating the complete circuit recovery-voltage characteristic obtained across the contacts of a circuit-interrupting device immediately after it has opened a faulted circuit. It is applicable to the usual circuit-breaker location on an electric-power system, and is believed to be sufficiently accurate for most practical purposes. The power system is represented by a few major circuits using lumped capacitances and inductances for apparatus and connections. These circuits are then reduced to the three or four principal circuits having the greatest influence on the voltage recovery, and the voltage transient across the open breaker contacts is obtained by combining the voltages and frequencies associated with these circuits. An appendix gives capacitance data for apparatus involved in the calculations.

42-136—Protection for Pilot-Wire Circuits; *E. L. Harder (M '41), M. A. Bostwick (A '42). 30 cents.* The best way to protect a pilot-wire circuit is to use construction such that the circuit is inherently free from induced voltages and is adequately shielded from lightning. These requirements are most easily met by using a twisted pair of wires in a lead-covered cable that is not on the power-circuit right of way. A circuit of this kind that is so insulated as to provide a good margin of safety from the maximum of difference in ground potential needs no other protection. However, in many applications these ideal conditions cannot be realized; hence some form of compromise must be made. The paper is written to guide those who must decide which compromise best suits their local conditions. It contains a brief discussion of pilot-wire requirements for different types of relays. The effects of induction from the power circuit, voltage stresses which result from differences in station ground potentials, and methods that can be employed to reduce these voltage stresses so as to insure correct operation of the associated relays are discussed.

Standards

42-124—Motor Insulation, Heat, and Moisture; *P. H. McAuley (A '36). 15 cents.* Among the foes of insulation are heat and moisture. Motor insulation subjected to these influences suffers to a certain extent, the evaluation of which has been difficult. In accelerated temperature and moisture tests on sample motors, insulation resistances and dissipation factors have been studied in an attempt to obtain better insulation condition indicators. These measuring sticks appear to have more consistency and significance than they have been given credit for and deserve wider use with more attention to measurement technique and analytical methods, but a great deal more work will be necessary to permit effective interpretation for apparatus under service conditions. Based on limited data, motor insulation appears to be more vulnerable to adverse moisture conditions than to moderately excessive temperatures, and available test methods are more useful in evaluating moisture effects on dielectric strength. Tentative minimum values of insulation resistance at room temperature permissible for starting are suggested for small motors.

PERSONAL • • • •

H. A. Wagner (A'98, M'03) president and chairman of the board of the Consolidated Gas, Electric Light, and Power Company of Baltimore (Md.) was recently re-elected chairman of the board, after resigning the presidency. He was born at Philadelphia, Pa., on February 24, 1867, and was graduated from Stevens Institute of Technology in 1887 with the degree of mechanical engineer. In that year he joined the engineering department of the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., and in 1890 he became general superintendent of the Missouri Electric Light and Power Company, St. Louis, continuing with its successor, the Missouri Edison Electric Company. In 1891 he established the Wagner Electric Manufacturing Company and became its first president. In 1899 he opened a consulting engineering office in St. Louis, Mo., and in 1900 he established a similar office in New York, N. Y., discontinuing his consulting practices in both cities in 1908 in order to become the director of the electric division of the Consolidated Gas, Electric Light, and Power Company of Baltimore. He served as vice-president and director from 1910 to 1915, when he was elected to the presidency. He is currently serving as a trustee of the Edison Electric Institute. **R. L. Thomas** (A'19, M'26) executive engineer of the company, has been elected a vice-president. He was born May 2, 1887, at Marion, Ohio, and received the degrees of bachelor of arts in 1909 from Princeton University and bachelor of science in electrical engineering from Massachusetts Institute of Technology in 1913. From 1913 to 1915 he was with Stone and Webster, Boston, Mass., and in 1915 he joined the Pennsylvania Water and Power Company with headquarters in Baltimore, Md. After an interval of military service (1917–19), he returned to the Pennsylvania Water and Power Company as an efficiency engineer, in 1923 becoming assistant to the general superintendent. Later he held successively the offices of assistant general superintendent and general superintendent. In 1930 he joined the Safe Harbor Water Power Corporation as project engineer, continuing his work with the Pennsylvania Water and Power Company until 1938, when he became executive engineer for the Consolidated Gas, Electric Light, and Power company. He was made a director of that company in 1939. He is also a member of The American Society of Mechanical Engineers and Tau Beta Pi.

D. C. Luce (A'36, M'36) general superintendent of generation, electric generation department, Public Service Electric and Gas Company, Newark, N. J., has been appointed general manager of the electric department. He joined the company in 1924 as a cadet engineer and in 1927 was made an assistant engineer in the testing department of the company's Marion generating station. In 1935 he was appointed chief engineer at the Kearny station and the following year he was made general superintendent of electric generation. **W. F. Tait** (A'27) assistant general superintendent of distribution, has been made assistant general manager of the electric department. He has been with the company since 1922. After two years as a cadet engineer, he became an engineer in the general office of the distribution department and in 1928 he became assistant distribution engineer. He has been assistant general superintendent of distribution since

1938. **W. R. LaMotte** (M'36) assistant general superintendent of generation, has been appointed general superintendent of electric generation. He joined the company in 1914 and was engaged in electrical construction work, south division, until 1917. After an interval of naval service, he returned to the engineering department in 1918. In 1921 he was appointed chief engineer of the company's Perth Amboy power station. He became chief engineer of the Marion station in 1925 and the Essex station in 1926, and was appointed assistant general superintendent of electric generation in 1941. **A. R. Nelson** (A'22, M'29) division superintendent of the Essex division, has been appointed assistant general superintendent of electric distribution. He entered the production department of the company's Marion power station in 1920 as a cadet engineer and became assistant superintendent of electrical distribution at Camden, N. J., in 1924. He became assistant to the division superintendent, southern division, Trenton, N. J., in 1927 and in 1931 he was appointed assistant transmission construction engineer. He was made assistant division superintendent of the Essex division in 1934 and became superintendent of the division in 1940.

David Levinger (M'30) works manager, Hawthorne Works, Western Electric Company, Chicago, Ill., has been elected a vice-president of the company. He was born on August 27, 1887, at Delta, Idaho. He was employed as a steel inspector by the International Harvester Company, Chicago, Ill., from 1907 to 1910, when he joined the Western Electric Company as a mechanical engineer. In 1918 he was appointed engineer of mechanical methods, and in 1920 he was appointed assistant technical superintendent in charge of mechanical, electrical, and chemical development at the company's Hawthorne plant. He was assistant superintendent of development from 1922 to 1925, when he became superintendent of manufacturing development. In 1928 he was transferred to New York, N. Y. as engineer of manufacture and in 1931 he was elected a director of the company. He returned to the company's Hawthorne plant in 1940 as works manager. He is also a member of The American Society of Mechanical Engineers, the American Institute of Mining and Metallurgical Engineers, and the American Association for the Advancement of Science. **F. R. Lack** (M'37) manager of the company's radio sales division has also been named a vice-president. He was born in Eastbourne, England, July 18, 1895, and was graduated from Harvard University with the degree of bachelor of science in 1925. In 1920 he was sent to China by the Western Electric Company as engineer in charge of installing radiotelephone equipment between Peking and Tientsin, returning in 1922. After studying at Harvard University, he joined the technical staff of Bell Telephone Laboratories in 1925. In 1935 he was placed in charge of vacuum-tube development and in 1937 he was appointed director of vacuum-tube development. He became manager of the specialty products division of the Western Electric Company in 1939 and was recently named manager of the company's radio sales division. He is also a member of the Institute of Radio Engineers.

H. C. Clemment (A'07, M'13) central-station department, General Electric Company, New York, N. Y., has been appointed assistant to the district manager of the New York district. He has been with the company continuously since 1910, when he was employed as sales engineer in the New York district. In 1919 he was transferred to the syndicate department and in 1923 he joined the sales division of the central-station department.

K. B. McEachron (A'14, F'37) research engineer, General Electric Company, Pittsfield, Mass., has been appointed a member of the Board of Registration of Professional Engineers and of Land Surveyors for the Commonwealth of Massachusetts, which was created by the legislature in October 1941. He represents electrical engineering.

A. A. Browne (A'38) former assistant manager of the central-station and transportation divisions of the Pacific Coast district of the Westinghouse Electric and Manufacturing Company, San Francisco, Calif., is now associated with the Moore Machinery Company, San Francisco, Calif.

O. J. Braun (A'39) draftsman, Amalgamated Electric Corporation, Ltd., Toronto, Ont., has been transferred to Vancouver, B. C., to join the staff of the Langley Electric Company which is associated with Amalgamated Electric Corporation.

C. M. Jansky, Jr. (M'32) recently appointed chief of the Radio Section of the Communications Branch of the War Production Board, has been transferred to special duty with the Army Air Force.

T. W. Hill (A'39) managing director, Hugh C. MacLean Publications, Ltd., Toronto, Ont., and editorial director of *Electrical News*, is on leave in New York, N. Y., with the British War Office.

S. E. Schultz (A'25, M'41) chief engineer, Bonneville Power Administration, Portland, Ore., is on leave of absence, serving as a power supply consultant with the Power Branch of the War Production Board.

C. C. Johnson (M'20) president, American District Telegraph Company, New York, N. Y., has been nominated to serve as a vice-president of the National Fire Protection Association.

OBITUARY • • • •

Rudolf Emil Hellmund (A'05, M'09, F'13) chief engineer, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., died at New York, N. Y., on May 16, 1942. Born at Gotha, Germany, on February 2, 1879, he received the degree of electrical engineer from the Technical College of Ilmenau, Germany, in 1899. He also did postgraduate work at the University of Charlottenburg, Germany, in 1901–02. In 1899 he joined the Sachsische Elektrizitatswerke, Poeschmann, Germany, to work on the design of electrical machinery. He entered the laboratory of the Land-und-See Kabelwerke, Cologne, Germany, in 1900, and in 1901 joined the Maschinenfabrik Eplingen, Stuttgart, Germany, as head of testing work. In 1903 he came to the United States, and became a United States citizen in 1920. In 1903 he joined the Krantz Company, Brooklyn, N. Y., as a designer, after which he was for a short time consulting engineer with the Hellmund and Colbohn Electric Company, New York, N. Y., before becoming a designer for the William Stanley Company, Great Barrington, Mass. In 1905 he became designer for the Western Electric Company, Hawthorne, Ill., and in 1907 joined the Westinghouse Electric and Manufacturing Company. From 1907 to 1909 he was engaged in the design of induction motors, from 1909 to 1911 in general engineering, and from 1911 to 1917 he was in charge of the design of all a-c and d-c railway motors. He carried on consulting and development work from 1918 to 1921, was made engineering supervisor of development in 1922, chief electrical engineer in 1926, and chief engineer in 1933. He held a number of patents on electrical devices and in 1929 was awarded the Lamme Medal for his contributions to the design and development of rotating machinery. Since 1939 he had been a director of the Institute. He had served as a member of the committee on planning and co-ordination and was Institute representative or alternate on the electrical standards committee and the standards council of the American Standards Association. He was serving currently also on the AIEE standards (chairman 1938–40), executive, Lamme Medal (chairman 1941), and Edison Medal committees, as well as the committee on applications of electricity to therapeutics. He was also a member of the United States National Committee of the International Electrotechnical Commission.

Clayton Halsey Sharp (A'02, M'12, F'12) consulting engineer, White Plains, N. Y., died on May 14, 1942. He was born at Seneca Falls, N. Y., on December 5, 1869, and received the degrees of bachelor of arts from Hamilton College in 1890, doctor of philosophy in 1895 from Cornell University, and an honorary degree of doctor of science from Hamilton College in 1941. He was an assistant and instructor in physics at Cornell University, Ithaca, N. Y., from 1894 to 1901, when he became test officer of the Electrical Testing Laboratories, New York, N. Y. In 1914 he became vice-president and technical director, which positions he held until his retirement in 1933. He had been a member of a number of Institute committees. He served the United States National Committee of the International Electrotechnical Commission, as a delegate at a number of international conventions, as secretary, 1921–23, and as president from 1924 to 1940, when he became honorary president. He was also president of the United States National Committee of the International Commission on Illumination (1914–28), was a past president of the Illuminating Engineering Society, and a member of the American Physical Society, American Association for the Advancement of Science, and the Société Française des Electriciens. He was also a member of Sigma Xi and Phi Beta Kappa and was the inventor of several instruments for electrical and photometric measurement and the author of many technical papers.

Herbert Edward Shreeve (A'06, M'21, F'30) retired technical representative in Europe for the American Telephone and Telegraph Company and Bell Telephone Laboratories, both of New York, N. Y., died on April 24, 1942. He was born at Cambridge, England, on August 1, 1873, and was graduated from Finsbury Technical Institute, London, in 1894. From 1889 to 1895 he was employed as an apprentice at the Telegraph Works, Silvertown, London, and after coming to the United States, he was employed in 1895 as a telephone engineer with the American Telephone and Telegraph Company, Boston, Mass. He was in the engineering department of the Western Electric Company from 1907 to 1917, working on the development of a transatlantic radiotelephone and telephone repeaters. After an interval of military service with the Signal Corps, he returned to the Western Electric Company in 1919 as staff engineer, and in 1923 he was appointed assistant to the vice-president of the company. In 1925 he became assistant to the president, Bell Telephone Laboratories, and from 1926 until his retirement in 1935 he was technical European representative for both the American Telephone and Telegraph Company and Bell Telephone Laboratories. He held a number of patents on telephone repeaters and transmission devices and was honored by the National Association of Manufacturers in 1940 as a "modern pioneer."

William David Wood (A'31) general plant supervisor, Southern Bell Telephone and Telegraph Company, Atlanta, Ga., died March 22, 1942. He was born at Suffolk, Va., September 29, 1885. In 1902 he joined the Southern Bell Telephone company in Suffolk, Va., and had been with the company continuously since that time except for a period (1914–18) when he was assistant field engineer with the engineering branch, division of valuation, Interstate Commerce Commission. In 1905 he went to Norfolk, Va., as an installer; the following year he was with the Bell Company of Pennsylvania, and shortly thereafter he went to Birmingham, Ala., as central office man. In 1910 he was made plant foreman at Tuscaloosa, Ala., and in 1911 he was transferred to Anniston, Ala., where he later became plant chief. He was with the engineering branch of the Southern Bell company, Atlanta, Ga., from 1918 to 1920, when he was appointed toll-lines engineer for Louisiana and Mississippi. In 1923 he became division engineer for Mississippi and Tennessee and in 1926 he became plant superintendent for Mississippi, Louisiana, and Kentucky, with headquarters at Jackson, Miss. In 1927 he took a similar position at the company's New Orleans plant and in 1930 he was made Kentucky plant superintendent with headquarters at Louisville. He was appointed general plant supervisor in 1935.

Ernest Maurice Siegel (M'42) assistant professor of electrical engineering at the University of Texas, Austin, died April 10, 1942. He was born at Svitavy, Moravia, on January 25, 1886, and received the degrees of master of science in engineering, and doctor of philosophy in electrical engineering in 1911 from the Technical University of Brno. From 1910 to 1920 he was an assistant professor at the Technical University of Brno, Czechoslovakia, and in 1920 became a professor at the Technical University of Prague, Czechoslovakia. In addition to teaching, he maintained a consulting practice and was a member of the state council for the electrification of Czechoslovakia from 1934 to 1939. He had also served as president of the municipal hydroelectric plant at Kadan, Bohemia. In 1939 he came to the United States as an instructor at the University of Texas and in 1941 became an assistant professor there. He was the author of a number of technical articles on power generation and transmission and radio receiving and transmitting equipment. He was also a member of Sigma Xi.

Harold Frederick Rice (A'21, M'32) associate professor of electrical engineering, University of North Dakota, Grand Forks, died March 24, 1942. He was born at Osceola, Iowa, on September 13, 1898, and received the degrees of bachelor of science in electrical engineering in 1920 and electrical engineer in 1927 from the University of Colorado. He entered the testing department of the General Electric Company in 1920 and in 1922 joined the a-c engineering department. From 1923 to 1928 he was employed as distribution engineer in the electrical engineering department of the Public Service Company of Colorado. In 1928 he went to the University of North Dakota as an instructor of electrical engineering, in 1929 he was appointed assistant professor of electrical engineering, and during 1929–30 he served as acting head of the electrical engineering department. He was AIEE Student Branch counselor at the University of North Dakota. He was also a member of the Society for the Promotion of Engineering Education, Tau Beta Pi, and Sigma Tau.

Otho Clarence Roff (A'07) sales engineer, central-station department, General Electric Company, Philadelphia, Pa., died on March 25, 1942. He was born March 10, 1883, at Pulteney, N. Y. In 1901 he entered the meter department of the General Electric Company, Schenectady, N. Y., and after a year at the Bliss Electrical School, from which he was graduated in 1903, he returned to the General Electric Company to work on experimental testing. During 1905–06 he was employed in the testing department and from 1907 to 1915 he was in the power and mining engineering department working on generator voltage regulators. In 1915 he was transferred to the company's Philadelphia plant as sales engineer.

Richard Herman Lang (A'21, M'27) superintendent of power-transmission stations, Consolidated Gas, Electric Light, and Power Company of Baltimore (Md.) died on April 4, 1942. He was born at Pittsburgh, Pa., on January 21, 1884. He joined the Chloride of Silver Dry Cell Battery Company in 1898 and in 1900 he was employed as an operator by the Consolidated Gas, Electric Light, and Power company at the Monument Street steam-generating station. From 1904 to 1906 he was operator at the McClellan Street d-c substation and from 1906 to 1909 he was operator at the Westport generating station. He was appointed chief operator in charge of electric stations in 1909.

Houston Alfred Dodson (A'37) former testing engineer, Remington Arms Company, Kansas City, Mo., died on January 23, 1942. He was born at Cordell, Okla., on August 13, 1913, and received the degree of bachelor of science in 1936 from Oklahoma Agricultural and Mechanical College. He joined the Southwestern Light and Power Company, Lawton, Okla., in 1936 as a calculating-board operator. He was later transferred to substation maintenance work and in 1937 was made a foreman. He was superintendent of electric meters from 1940 to 1941, when he joined the Remington Arms Company as plant electrical maintenance superintendent.

Morris S. Towson (A'08) president and general manager, Elwell-Parker Electric Company, Cleveland, Ohio, died on March 17, 1942. He was born at Cleveland, Ohio, in 1865 and received the degrees of bachelor of science and civil engineer from the Case School of Applied Science in 1886. From 1886 to 1896 he worked as a designing and construction engineer on electric and cable railways and in 1896 he joined the Elwell-Parker Electric Company as electrical engineer, later becoming shop superintendent. He became general manager of the company in 1907 and later was elected to the presidency.

James Albert Cranston (A'08) retired commercial vice-president of the General Electric Company, San Francisco, Calif., died on April 15, 1942. He was born on April 7, 1862, at Bayfield, Ont., Canada. In 1889 he was employed by the Thomson-Houston Company, St. Paul, Minn., and when that company became a part of the General Electric Company in 1892, he became manager of the northwest territory with headquarters in Portland, Oreg. In 1923 he was transferred to San Francisco as manager of the company's Pacific Coast district and in the same year he was elected a commercial vice-president.

Howard D. Clark (A'32) cadet engineer, Connecticut Light and Power Company, New Britain, died January 21, 1942. He was born April 15, 1903, at Yonkers, N. Y., and attended Rensselaer Polytechnic Institute. In 1926 he joined the Connecticut Light and Power Company as a meter tester and in 1927 he was made assistant operator of the company's Black Rock substation. Later in that year he was appointed cadet engineer.

MEMBERSHIP • •

Recommended for Transfer

The board of examiners, at its meeting on May 21, 1942, recommended the following members for transfer to the grade of membership indicated. Any objections to these transfers should be filed at once with the national secretary.

To Grade of Fellow

Bolton, F. C., dean of college, Texas A. and M. College, College Station, Texas.
Buttolph, L. J., application engineer, General Electric Company, Cleveland, Ohio.
Harwood, P. B., manager of engineering, Cutler-Hammer, Inc., Milwaukee, Wis.
Lebenbaum, Paul, electrical engineer, Southern Pacific Company, San Francisco, Calif.
Richter, Walther, consulting electrical engineer, Milwaukee, Wis.
Silsbee, F. B., physicist, National Bureau of Standards, Washington, D. C.

6 to grade of Fellow

To Grade of Member

Clark, L. W., engineer, Detroit Edison Company, Detroit, Mich.
Clothier, G. W., sales engineer, Allis-Chalmers Manufacturing Company, Milwaukee, Wis.
Cohn, Nathan, district manager, technical sales, Leeds and Northrup Company, Chicago, Ill.
Cotner, W. W., assistant professor of electrical engineering, Cornell University, Ithaca, N. Y.
Davidson, L. A., relay engineer, Oklahoma Gas and Electric Company, Oklahoma City, Okla.
Dimity, C. D., electrical engineer, Phelps Dodge Copper Products Corporation, Chicago, Ill.
Glasgow, R. S., professor of electrical engineering, Washington University, St. Louis, Mo.
Irish, C. V., electrical engineer, Electric Boat Company, Bayonne, N. J.
Lank, W. J., system planning engineer, Potomac Electric Power Company, Washington, D. C.
Lytle, C. M., assistant superintendent, Kansas City Power and Light Company, Kansas City, Mo.
McDonald, J. W., electrical design engineer, Commonwealth and Southern Corporation, Jackson, Mich.
Mosley, C. E., electrical engineer, W. N. Matthews Corporation, St. Louis, Mo.
Oldacre, M. S., equipment and research engineer, Commonwealth Edison Company, Chicago, Ill.
Rogers, J. H., electrical engineer, Eastman Kodak Company, Rochester, N. Y.
Stevens, A. M., radio engineer, International Telephone and Telegraph Company, New York.
Trotter, J. M., engineer, Commonwealth and Southern Corporation, Jackson, Mich.
Vincent, H. L., assistant manager, Westinghouse Electric Supply Company, Spokane, Wash.
Waits, C. E., engineer, Commonwealth and Southern Corporation, Jackson, Mich.
Wilhelm, G. R., general plant engineer, Chesapeake and Potomac Telephone Company, Washington, D. C.

19 to grade of Member

Applications for Election

Applications have been received at headquarters from the following candidates for election to membership in the Institute. Names of applicants in the United States and Canada are arranged by geographical District. If the applicant has applied for direct admission to a grade higher than Associate, the grade follows immediately after the name. Any member objecting to the election of any of these candidates should so inform the national secretary before June 30, 1942, or August 31, 1942, if the applicant resides outside of the United States or Canada.

United States and Canada

1. North Eastern

Baker, G. E., Eastman Kodak Company, Rochester, N. Y.
Callinan, T. D., General Electric Company, Pittsfield, Mass.
Cataldo, A. R., Western Electric Company, Watertown, Mass.
Cookson, L. B., Submarine Signal Company, Boston, Mass.
Ericson, E., J-B-T Instruments Inc., New Haven, Conn.
Herrick, H. E. (Member), American Woolen Company, Providence, R. I.
Holihan, T. D. (Member), 500 Bryant Avenue, Syracuse, N. Y.
Joyal, H. J., General Electric Company, Pittsfield, Mass.
O'Leary, D. F. (Associate re-election), Boston Edison Company, Boston, Mass.
Suuronen, E., General Electric Company, Bridgeport, Conn.
Votaw, R. W., General Electric Company, Schenectady, N. Y.
Weiss, I., General Electric Company, Schenectady, N. Y.
Worcester, W. G., General Electric Company, Schenectady, N. Y.

2. Middle Eastern

Angello, S. J., Westinghouse Research Laboratories, East Pittsburgh, Penna.
Bacheler, A. T., Westinghouse Electric and Manufacturing Company, E. Pittsburgh, Pa.
Bania, S. E., War Department, Signal Corps, Philadelphia, Pa.
Brauch, H. N. (Associate re-election), Westinghouse Electric and Manufacturing Company, Sharon, Pa.
Brewer, G. E., Westinghouse Electric and Manufacturing Company, Sharon, Pa.
Brown, J. D., General Electric Company, Erie, Pa.
Christensen, M. M., Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
Cramer, H. J. A., Industrial Rayon Company, Cleveland, Ohio.
Cunningham, G. T., Bethlehem Steel Company, Baltimore, Md.
Cunningham, R. C., Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
D'Azzo, J. J., Army Air Corps, Wright Field, Dayton, Ohio.
Deflenbaugh, J. F., The Ohio Public Service Company, Sandusky, Ohio.
Flynn, M. E., Jr., Robbins and Myers, Inc., Springfield, Ohio.
Fredrickson, E. H., I-T-E Circuit Breaker Company, Philadelphia, Pa.
Howell, E. H. (Associate re-election), General Electric Company, Toledo, Ohio.
Murphy, H. S. (Member), Philadelphia Transportation Company, Philadelphia, Pa.
Pullen, C. C., Bethlehem Steel Company, Bethlehem, Pa.
Pullen, K. A., Jr., Johns Hopkins University, Baltimore, Md.
Sproul, W. W., Jr., Westinghouse Electric and Manufacturing Company, Sharon, Pa.
Stanton, C. C., United States Army Signal Corps, Fort Hayes, Columbus, Ohio.
Stark, C. H., Stark Electric Company, Baltimore, Md.
Swanker, W. C., Ohio Bell Telephone Company, Cleveland, Ohio.
Taylor, C. O., Cincinnati Gas and Electric Company, Cincinnati, Ohio.
Trouant, V. E., RCA Manufacturing Company, Inc., Camden, N. J.
Wolfe, R. W., General Electric Company, Philadelphia, Pa.

3. New York City

Bokum, W. H. (Associate re-election), George S. Armstrong and Company, Inc., New York, N. Y.
Charton, S., Signal Corps, Fort Monmouth, Red Bank, N. J.
Creedon, H. T., New York Telephone Company, New York, N. Y.
Eagan, J. F., Jr., Chemical Construction Corporation, New York, N. Y.
Fairweather, B. A., Bell Telephone Laboratories, Inc., New York, N. Y.
Hunsicker, J. P., U. S. Electrical Motors, Inc., New York, N. Y.
Jutson, R. P. (Member), Bell Telephone Laboratories, Inc., New York, N. Y.
Kalantar, H. H. (Member re-election), J. G. White Engineering Corporation, New York, N. Y.
Lauraitis, A. J., Fire Department, New York, N. Y.
McCutchan, J. F., New York Telephone Company, Brooklyn, N. Y.
Meagher, R. H., Control Instrument Company, Inc., Brooklyn, N. Y.
Osiatinsky, L. (Member), International General Electric Company, Inc., New York, N. Y.
Peragallo, J. T., Western Electric Company, Kearny, N. J.
Potter, J. E. (Member), John E. Potter Company, New York, N. Y.
Randazzo, P. M., United States Signal Corps, Governors Island, N. Y.
Scheick, E. H., Ford, Bacon and Davis, Incorporated, New York, N. Y.
Schjelderup, J. R., Western Electric Company, Kerny, N. J.
Underhill, E. A., Western Electric Company, Inc., Kearny, N. J.
Wilhelm, F. A., Western Union Telegraph Company, New York, N. Y.

4. Southern

Hynes, R. R., Ashland Oil and Refining Company, Ashland, Ky.
Jones, S. J., Aluminum Company of America, Alcoa, Tenn.
Martin, A. S., Corps of Engineers, United States Army, Jacksonville, Fla.
McClinton, A. T., Virginia Polytechnic Institute, Blacksburg, Va.
Naiman, R. D., Tennessee Valley Authority, Fort Loudoun Dam, Lenoir City, Tenn.
Parham, W. L., Aluminum Company of America, Alcoa, Tenn.
Polk, W. E. (Member), Southwest Louisiana Trade School, Lake Charles, La.
Purinton, I. H., New Orleans Public Service, Inc., New Orleans, La.
Taylor, D. P., Woodard Wright and Company, Ltd., New Orleans, La.

5. Great Lakes

Clyde, J. P. (Associate re-election), Public Service Company of Northern Illinois, Maywood, Ill.
Enochs, J. R., General Electric Company, Fort Wayne, Ind.
Gardner, G. N., United States Rubber Company, Mishawaka, Ind.
Howard, A. W. (Member), General Electric Company, Ft. Wayne, Ind.
Larson, H. E., General Electric Company, Chicago, Ill.
Morgan, A. L., Commonwealth and Southern Corporation, Jackson, Mich.
Peary, D. R. (Member), Chicago Bridge and Iron Company, Chicago, Ill.
Raftree, L. K., The Adams and Westlake Co., Chicago, Ill.
Raymer, D. R., Commonwealth and Southern Corporation, Jackson, Mich.
Stratton, D. R. (Member), Commonwealth and Southern Corporation, Jackson, Mich.
Zurawski, S. W., 8247 Forestlawn, Detroit, Mich.

7. South West

Anderson, O. K., Camp Swift, Bastrop, Texas.
Boyer, O. A. (Member), Oklahoma Power and Water Company, Sand Springs, Okla.
Douglas, C., Camp Swift, Bastrop, Texas.
Jenkins, O. T., Santa Fe Bldg., Dallas, Texas.
LeVee, C. H. (Member), Ford, Bacon and Davis, Inc., Little Rock, Ark.
Pevoto, I. S., Gulf States Utilities Company, Baumont, Texas.
Robertson, G. E., Gilbert and Elgin Robertson, Sales Engineers, Dallas, Texas.
Shackelford, C. L., University of Missouri, Columbia, Mo.

8. Pacific

Anderson, F. W., United States Naval Reserve, San Francisco, Calif.
Chase, L. R., United States Bureau of Mines, Boulder City, Nevada.
Farrar, H. K., Southern California Telephone Company, Los Angeles, Calif.
Galles, B. P., Holmes and Narver, Santa Ana, Calif.
Hesse, J. F. (Member), Imperial Irrigation District, Imperial, Calif.
Triay, O. G., Consolidated Steel Corporation, Wilmington, Calif.

9. North West

Hansen, D. S., Morrison-Knudsen Company, Inc., Pocatello, Idaho.
Hoopes, D. R., Westinghouse Electric and Manufacturing Company, Salt Lake City, Utah.
Howe, C. D. (Associate re-election), The Pacific Telephone and Telegraph Company, Seattle, Wash.
Mauk, C. E. (Member), Lippincott, Bowen, and Rowe, Salt Lake City, Utah.

10. Canada

Collins, K., Eagle River Power Station, Eagle River, Ont.
Dawson, G. H. (Member re-election), Ottawa Group of Architects, Ottawa, Ont.
Murray, J. D., Electrical News and Engineering, Toronto, Ont.

Total, United States and Canada, 98

Elsewhere

Conangla, A., University of Havana, Havana, Cuba.
Krancus, A. F., U. S. Army, Honolulu, T. H.
Ramanna, B., 1792, Padmalaya, Malleswaram Post, Sampige Road, Bangalore, India.

Total, elsewhere, 3

OF CURRENT INTEREST

Policies and Procedures of War Manpower Commission

Finding some 20,000,000 workers and placing them in war production jobs is the huge task faced by the recently appointed War Manpower Commission of which Paul V. McNutt, Federal Security Administrator, has been appointed chairman. Other members of the commission and the agencies they represent are:

Goldthwaite H. Dorr, War Department.
James V. Forrestal, Navy Department.
Claude R. Wickard, Agriculture Department.
Frances Perkins, Labor Department.
Donald M. Nelson, War Production Board.
Wendell Lund, labor production division, War Production Board.
Lewis B. Hershey, Selective Service System.
Arthur S. Flemming, Civil Service Commission.

In addition, Chairman McNutt recently announced the appointment of two ranking officers of the commission, Fowler V. Harper, deputy chairman; and Arthur J. Altmeyer, executive officer.

From announcements made when the commission was appointed and subsequently, the basic policies and procedures of the commission in fulfilling its duties are beginning to take shape. Upon his appointment, Chairman McNutt issued a statement in which he said, in part:

"Through this commission, all the agencies of the Federal Government, working closely with representatives of labor and industry, will be able to develop and maintain co-ordinated labor-supply policies and programs on a voluntary and democratic basis. At the present time I can see no need for the building up of a large administrative staff to do this work. I believe that we can carry on most of the functions through the existing agencies.

"The Commission has been charged by the President with the formulation of plans and programs and the establishment of basic national policies to assure the most effective mobilization and maximum utilization of the nation's man power in the prosecution of the war, and the issuance of such policy and operating directives as may be necessary.

"Its other functions will include: 'estimating the requirements of man power for industry, reviewing all other estimates of needs for military, agricultural and civilian man power, and directing the several departments and agencies of the Federal Government as to the proper allocation of available man power.'

"It will determine basic policies for the collection and compilation of labor market data by Federal departments and agencies, and it will establish policies and prescribe regulations governing all Federal programs relating to the recruitment, vocational training, and placement of workers to meet the needs of industry and agriculture.

"It will also prescribe basic policies governing the filling of the Federal Government's requirements for man power, excluding those of the military and naval forces, and issue such operating directives as may be necessary.

"Finally, it is charged with formulating legislative programs designed to facilitate the most effective mobilization and utilization of the man power of the country. . . .

"I want to emphasize that in my opinion the primary reason for my designation as chairman of this commission is that many of the activities with which the commission will be concerned are already operating within the Federal Security Agency. These include the United States Employment Service, with its basic responsibility for recruiting labor, and the wartime training programs carried out through the United States Office of Education, the Civilian Conservation Corps and the National Youth Administration. . . .

"These are among the steps the commission will have to consider:

"1. We shall have to decide where our available labor supply is most urgently needed.

"(*a*) We shall have to make a careful determination of the man power and woman power required by the armed forces, by war industry, by agriculture, by transportation, and by civilian production.

"(*b*) We shall have to obtain the necessary information concerning occupational skills possessed by each man and woman to make sure that all of us are serving where we are most urgently needed. These data are now being obtained from Selective Service registrants through an occupational questionnaire.

"(*c*) Arrangements will have to be made for supplying labor to various war industrial plants in accordance with the urgency of the need for the products each plant turns out.

"2. In order to obtain the most effective use of our limited supply of skilled workers we must make sure that:

"(*a*) Employers use their skilled workers only at jobs where such skills are required.

"(*b*) Skilled workers are hired through an orderly process guaranteeing that factories engaged in the most urgent war production receive first call on the available supply.

"(*c*) Man power is allocated between the armed forces and industry in such a way as to provide for most effective utilization of men whose skills are essential to the war production.

"3. In order to make full use of presently unemployed workers we must make sure that:

"(*a*) War contracts are placed in areas in which there are now large numbers of unemployed and that further concentration of war production is avoided in areas of labor shortage.

"(*b*) Effective measures are enforced to insure full utilization of local labor.

"4. In order to mobilize the full man power and woman power of the country to meet the prospective needs of the war program we must:

"(*a*) Utilize women in industries where they are best fitted to serve, and under proper working conditions.

"(*b*) Utilize on a full-time basis in war industry workers who are now partially employed.

"(*c*) Expand our program of industrial training as fully as possible.

"(*d*) Tap all available labor supplies, such as the millions of negroes and loyal foreign-born workers not now in war production.

"Most of the 13,000,000 war production workers who will be placed in jobs during the next year will come from those who are now employed in nonwar industries. . . . Other workers will be drawn from the unemployed. We shall also need at least several million new recruits—women, young people, self-employed persons, and retired workers."

Although detailed procedures still remain to be formulated, it is reported that labor will not be conscripted, but that there will be rationing of labor on a voluntary basis. This will be carried out through Federal employment offices which will handle the hiring of workers for all war industries. These offices will have the individual occupation records of every man from 20 to 65 years old. Efforts will be made to employ workers in their own communities and to keep to a minimum the migration of workers from one community to another.

There appears to be a definite movement in the direction of a "work or fight" policy on the part of the Selective Service System. Men under 45 years of age with dependents are becoming increasingly likely to be called into service when engaged in nonessential occupations. It is expected that men in that classification will be urged to seek employment in essential industries.

Employment of women in industry is expected to increase greatly in the future, but industry is not ready for any mass employment of female help at the present time. However, Chairman McNutt in a recent statement said that women "will be trained and employed in war and essential civilian industries at an increasing rate during the next two years."

The induction of women will be along voluntary lines, utilizing first the women with industrial experience, and training women who are available for such work, Mr. McNutt said. No early necessity of a nation-wide registration of all women is foreseen, he added, pointing out that the United States Employment Service has 1,500,000 women registered already who are looking for jobs. Many of these are qualified for war-industry employment. He predicted that a million or more additional women will be employed in war industries this year, and that 1943's expansion of war production will bring women into war jobs rapidly to a probable total of 4,000,000 out of an expected total of 20,000,000 or more war workers.

Mercury Rectifiers Speed Metals Production

Increased needs in war production for aluminum and magnesium, which are produced by electrochemical processes requiring direct current, have caused increased production of mercury-arc rectifiers. Here Doctor Joseph Slepian (F'27) associate director of research, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., is shown (left) with W. E. Pakala (A'38) Westinghouse engineer, inspecting the vacuum gauge of an ignitron rectifier, Westinghouse product of which Doctor Slepian is coinventor

Engineer's Job in War Production Theme of "All-Engineers" Dinner

A plea from Lieutenant General Brehon B. Somervell, commander of the Army's services of supply, for engineering assistance in attacking "our greatest bottleneck," transportation; and a plea from Chairman William L. Batt of the War Production Board's requirements committee for the salvage and saving of strategic materials were voiced in Washington, D. C., before an audience of engineers Monday evening, May 18, 1942. The occasion was an "all-engineers" dinner, jointly sponsored by the Washington, D. C., units of 16 engineering societies, which brought a crowd of more than 1,200 engineers and their guests to overflow the ballroom at the Mayflower Hotel. James W. Parker, president of The American Society of Mechanical Engineers, officiated as toastmaster. Scheduled speakers included William L. Batt, Lieutenant General Brehon Somervell, and Donald M. Nelson, chairman of the War Production Board. Mr. Nelson was unable to be present.

TRANSPORTATION BOTTLENECK

General Somervell placed the nation's war transportation problems on such an important and critical level that he stated with considerable feeling that the person who could solve these problems would be "as great a national hero as the general who wins battles on the field." He said further, in part, "I especially commend to the attention of those of you in transportation the unhappy fact that transport afloat and ashore is our greatest bottleneck. Our tank factories and our plane factories are turning out machines in numbers that would astound and dismay the dictators. Our task is to get them where they are needed by the shortest route in the shortest possible time."

As for the engineer's job in war production, General Somervell called upon his audience to look for every opportunity to present their individual constructive ideas promptly to whatever transportation agency such ideas would serve best, "be it railroad, building construction, steamship, or tank arsenal."

THE MATERIALS QUESTION

Mr. Batt told his audience that he was "convinced that there is enough material in this country to win the war," but he stated that he also was "convinced that this material must be husbanded carefully by intelligent people so that not a pound is wasted."

"The importance today of scrap can hardly be exaggerated," emphasized Mr. Batt, as he called upon engineers to give assistance in the conservation of war materials through the development of what he called "the mines above the ground"—by saving waste material and by better utilization of all strategic materials. He called attention also to the important part that "better technique and better ingenuity" can play in the conservation of steel, alloys, and other critical materials.

Efforts are being made to procure manuscript copies of these addresses so that they can be reported upon more extensively in a later issue of *Electrical Engineering*.

Wartime Safety Organization Formed. Stating that it is a step forward in overcoming the enormous and growing waste of man power through industrial accidents, Donald M. Nelson, WPB chairman, has expressed approval of a nation-wide safety movement which aims to control serious interruptions in war production caused by accidents. The movement, launched by businessmen and industrialists, is being sponsored by a new organization called the War Production Fund to Conserve Man Power, of which W. A. Irvin, former president of the United States Steel Corporation, New York, N. Y., has been appointed chairman.

Industrial Consultants Provided for Armed Services

To speed the program of converting additional industrial facilities to war production, the services of industrial consultants in the bureau of industry branches of the War Production Board are being made available to the Army, Navy, and Maritime Commission, a recent announcement by WPB Chief Donald M. Nelson states. Executives with production and engineering experience and direct personal knowledge of their industries have been brought into the government service, usually on a temporary basis. Their chief function is to furnish information to government procurement officers about the types of war work each industry can handle, either in prime contracts or in subcontracts. They do not enter into direct negotiations for placing orders. When special facilities are needed immediately, procurement officers refer to the industrial consultants, who can often tell where the facilities may be found, thus avoiding delays for the construction of new buildings or equipment.

The objective announced by the WPB is the conversion to war production of every usable facility in every manufacturing industry not required for minimum civilian needs. The present program emphasizes the use of existing tools and production lines, instead of the changeover of plants that would require valuable time to be consumed in retooling. Time does not permit a detailed program of placing war orders with every company affected by WPB limitation or conservation orders, since in many cases civilian production facilities are not readily adaptable to war needs, the announcement stated.

WPB Curtails Individual Preference Ratings

As a further step toward putting American industry under the production requirements plan, the War Production Board will soon discontinue granting preference ratings on individual applications for material to be used in general manufacturing operations, a recent announcement states.

Effective immediately, no individual application from a manufacturer for materials to be incorporated in his products over a period of more than one month will be approved. As previously announced, virtually all American industries requiring priority assistance are expected, for the quarter beginning July 1, to file a single application to cover all of their materials requirements for a calendar quarter, or for the remainder of a calendar quarter, when the application is filed in an interim period.

It has been the practice of some producers, who need priority assistance for only a few of the materials which they use, to file applications from time to time on individual PD-1A forms. The new policy will restrict the amount of materials to which a preference rating may be assigned in this way. It will provide WPB with a tighter check on the volume and uses of materials for which preference ratings are assigned, and will also require all applicants who need priority assistance in the regular course of their business to furnish full inventory information to WPB.

Producers whose annual volume of business amounts to less than $100,000 may file their PRP applications on a simplified form PD-25X. All others must use the regular PD-25A application.

Power-Supply Controls Set Up to Handle Shortages

Machinery to handle power shortages wherever and whenever they occur in the United States has been set up by the War Production Board, according to recent WPB announcement. Shortages of electric power have already occurred in some areas and are threatened in others. These shortages are generally caused by the vast increase in use of electric power by war industries, causing many systems to use their reserves in current operations, and reduce their margin in the event of breakdown, drought, fuel shortages, or other unfavorable conditions.

An order has been issued to assure a steady flow of power to war industries and essential civilian services by curtailing nonessential uses. Curtailment of electricity for regular consumers, however, will not take place until an area becomes a power-shortage area. Two main lines of action are set out in the order. The first, which goes into operation at once, requires utilities to operate their systems in a way that will produce the maximum amount of power from their present capacity. In general, this calls for integrating or tying together systems to permit transfer of power from one locality to another where the power is needed most. Operations include making available the maximum amount of power at peak periods, using water power as much as possible to save fuel, and maintaining as much reservoir storage as possible. No utility is permitted to abandon any of its generating facilities except upon authorization by the WPB. Each utility is required to ascertain the amount and availability of any electric power-generating facilities in the area owned by a nonutility power producer and make arrangements to connect such private capacity with the utility's system. The second part of the program, which will be put into operation when and where a shortage occurs, establishes machinery for mandatory curtailment of power for commercial and industrial consumers. Provision is also made for curtailment to residential consumers. When a power shortage develops, the Director of Industry Operations will define the power-shortage area, will establish emergency curtailment schedules, and put into operation any or all of the following power-saving programs:

1. Eliminate all nonessential lighting, such as sign lighting, show window lighting, flood lighting of athletic fields, and restrict lighting in stores and other public establishments to one watt per square foot of floor space.

2. Restrict or prohibit the use of electric power during peak periods. Such action would affect all consumers.

3. Limit delivery of power to an industrial customer to a power usage which will be based on a percentage of his highest demand during the 12 months from May 1, 1941 to April 30, 1942.

4. Restrict the consumption of large nonresidential consumers using more than 2,500 kilowatt-hours a week to a weekly quota based on a percentage of their power consumption in April of 1942 or any other period fixed by the WPB.

5. Restrict the consumption of consumers (residential and small commercial) using 2,500 or less kilowatt hours on a basis to be fixed at the time of the shortage.

Consumers exempt from the curtailment include governmental and community services, such as street lights and power for hospitals and schools; transportation services; communications services; military establishments; and plants engaged exclusively in war production.

WPB Sets Up Regional Offices

Continuing the policy of decentralizing its activities, the War Production Board has established 13 regional offices throughout the United States, according to announcement. The existing 120 field offices (*see EE, Feb. '42, p. 99–100*) are to be allocated as branch offices among the regional offices for administrative purposes. Management of regional and branch offices remains in the field operations bureau of the Division of Industry Operations, WPB. Policy and programs will be determined in Washington, but direction of more and more WPB activities in the field is expected, the announcement states. The regional offices are located in the following cities: Atlanta, Ga.; Boston, Mass.; Chicago, Ill.; Cleveland, Ohio; Dallas, Tex.; Denver, Colo.; Detroit, Mich.; Kansas City, Mo.; Minneapolis, Minn.; New York, N. Y.; Philadelphia, Pa.; San Francisco, Calif.; Seattle, Wash.

WPB Says "Buy Coal Now"

All consumers of coal, and especially industrial users and war plants, have been asked, in an open letter from Chairman Donald M. Nelson of the War Production Board, to stockpile coal at once to the limit of storage capacity. The letter points out that, while transportation difficulties are increasing, the railroads and other transportation facilities still have some surplus capacity which must be utilized now to avoid difficulties later, and warns that, unless this is done, "there will undoubtedly be serious transportation problems, probably involving emergency rationing, which might otherwise be avoided."

Additional factors in the situation are the fuel oil shortage on the Atlantic Coast and winter weather transportation problems, which make stockpiling operations now of great importance to all consumers who are or may be dependent on coal for heating; and the fact that this year's consumption of coal will be much greater than normal. Retail dealers who require financial help in purchasing coal for shipment before August 1, 1942, may apply for loans to the Reconstruction Finance Corporation or local banks, Mr. Nelson's letter states.

War Status of Television Probably Experimental

Action taken at an informal conference on television held by the Federal Communications Commission on April 9, 1942, indicates the probability of the suspension of commercial television during the war. Priorities resulting in the lack of materials for television transmitters, receivers, and replacement tubes and a shortage of television engineers caused by government and industrial war activities, it was pointed out, necessitate the placing of television on a developmental basis. A further deterrent is the recent War Production Board ban on construction which prevents the erection of new transmitters. Continuation of the temporary television transmitting standards (*EE, Mar. '41, p. 145–7*), during wartime was recommended by the Radio Manufacturers Association, with a reduction of television broadcasting to one or two hours weekly. The feasibility of television broadcasting for the transmission of instructions to air-raid wardens, for recruiting, Red Cross, and other war activities is to be referred to the government for determination.

Intensive Campaign Under Way to Salvage Scrap

The Bureau of Industrial Conservation of the War Production Board has introduced an intensive campaign to salvage all possible iron and steel scrap, nonferrous metal scrap, paper, burlap, and cotton and woolen rags, and other essential materials. These materials are needed as never before. Serious shortages exist. In a few cases, open hearth furnaces have been forced to close down because of the lack of scrap.

Although the collection of scrap is a normal procedure in industry, the usual methods employed do not suffice to produce the amount needed to fill the demands of the war program. The present program demands not only scrap from manufacturing processes, but from the wrecking of obsolete and abandoned machinery, structures, buildings, etc. The prompt and continual flow of scrap from all types of manufacturing plants, mills, mines, utilities, and municipalities, and the collection of scrap from homes, farms, schools, etc., are essential.

The following suggestions should be helpful to those interested in increasing their efforts and effectiveness:

1. Instruct all employees to be on the lookout for scrap and to bring it to a central point.
2. Make some one person responsible for hunting up all scrap and waste materials.
3. See that the person appointed gets materials moving quickly into proper channels.
4. Because scrap accumulates, keep the scrap-collecting program going.
5. Reuse salvaged equipment wherever possible. Reduce waste to a minimum.
6. Do not mix scrap metals; segregate as to kind.
7. Make scrap collection and salvage part of the housekeeping program.

Vast War Production in 1942 Scheduled by General Electric

Production of approximately a billion dollars worth of diversified war materials by the General Electric Company during the current year was recently predicted by C. E. Wilson, president, at the annual stockholders' meeting at Schenectady, N. Y. That this goal may be reached, he stated, is indicated by the fact that the March output, at an annual rate of $860,000,000, was achieved without the aid of new plants, which have been completed recently or which are now in construction, and without complete use of recently added facilities. It was further reported that in the production of almost all important war goods, the company was meeting schedules set by the service departments of the government and that the production of a number of outstanding items is ahead of schedule. In the interests of the increased production necessary for the war effort, the General Electric Company has expressed its willingness to make available for the duration of the war any of its designs and techniques, patented or unpatented, to other manufacturers of goods for the Army and the Navy.

Wartime Vacation Policy Stated by WPB

In response to inquiries from industry and labor, Chairman Donald M. Nelson of the War Production Board recently issued the official WPB policy in regard to vacations in wartime, essential substance of which was as follows:

> Experience here and abroad is indicating that the worker, even when stimulated by the urgency of the war situation, cannot work long hours and maintain peak output indefinitely. It is well-known that he benefits in peacetime from an annual vacation. After the extensive overtime and the added emotional strain of the war effort, it is certain that a rest period this year will prove doubly effective in the restoration of his energy and determination.
>
> In planning the vacation program in 1942, it is particularly necessary that American industry secure vacation benefits without paying a counterbalancing cost in productive hours lost. This can be accomplished by doing work ahead in the departments affected, by further overtime of the workers not on vacation, by a special program of training substitutes for those workers scheduled to go on vacation, and by the spreading of the vacations over a longer period where possible.

WPB Asks Curtailment of Street Lighting. The War Production Board power branch has asked all electric utility systems to discontinue for the duration of the war all street-lighting extensions except those needed for public safety, according to a recent announcement, which also states that agreements between utilities and governmental agencies providing for street and highway-lighting extensions should be suspended and that applications for priority assistance in obtaining material for such extensions should not be made. These requests do not apply to illumination or traffic-control signals for areas where traffic conditions are congested because of war industries, camps, and airports. Applications for priority assistance where installations are essential to public safety will continue to receive consideration by the WPB power branch.

INDUSTRY • • • • •

Test-Run for Pendulum-Suspension Cars. Three railroad coaches of the "pendulum"- or above-gravity-suspension type, built by the Pacific Railway Equipment Company, recently carried railroad officials and others on a test run between Chicago and Galesburg, Ill., on which a top speed of 105 miles per hour was reached. To demonstrate their riding qualities in comparison with other types of coaches, the "pendulum" cars were attached behind two stainless-steel lightweight cars and a conventional steel coach. Regarded as providing a new high in riding comfort, it has been predicted that the new cars will become a standard design after the war. A description of the "pendulum"-suspension car, then in the experimental stage, appeared in the January 1941 issue of *Electrical Engineering*, page 15.

Allis-Chalmers Elects President. Walter Geist, former executive vice-president of the Allis-Chalmers Manufacturing Company, Milwaukee, Wis., was elected president at a meeting of the board of directors on May 7, 1942, according to an announcement made by M. W. Babb, chairman of the board. Mr. Geist, who joined the company in 1909 as an errand boy in the Saw Mill engineering department, succeeds W. C. Buchanan, who recently resigned. In the Saw Mill engineering department he advanced to the position of engineer-in-charge. He became active in the development of a multiple V-belt drive for industrial use and in 1928 was made assistant manager of the milling department. He became a vice-president in 1939 and executive vice-president in 1942.

Sir William Bragg, Physicist, Dies. Sir William Henry Bragg, director of the Royal Institution, Fullerton professor of chemistry and director of the Davy-Faraday Research Laboratory, London, England, died on March 12, 1942. He was born in 1862 and was graduated from Trinity College, Cambridge, in 1884. He was widely known for his studies in atomic and molecular structures, and for his work in popularizing science. In 1915 he received the Nobel Prize jointly with his son, in 1930 he was awarded the Franklin gold medal of the Franklin Institute, and in 1936 he was awarded the Faraday Medal of the Institution of Electrical Engineers of Great Britain.

GE Issues Welding Instruction Film. "The Inside of Arc Welding" a 16-millimeter sound motion picture produced in color for General Electric Company, is being released for use by private, public, and industrial welding schools, and government agencies. The film is in six parts, each of which is a complete unit, taking ten minutes to show. Part I is already being distributed; the remaining five parts are expected to be released during June. Charts, animation, demonstrations by expert operators, close ups of the arc in action, and cross-sections of good and bad welds are used in the films to explain and emphasize the principles of welding. Information about the films, which may be obtained for single showings, or purchased at the cost of the prints, may be secured from the Visual Instruction Section of General Electric Company, Schenectady, N. Y., or from any General Electric office.

Co-operative Network Calculator at Purdue. Purchased co-operatively by Purdue University and six midwestern power companies, an $85,000 network calculator is being installed at the University, for use in planning the expansion of power systems serving war industries in Indiana, Ohio, and Kentucky. By creating and testing existing or proposed systems in miniature and performing in an hour calculations that would take an engineer nine months to make on paper, the network calculator speeds power-system expansion and keeps new construction at a minimum, experience has shown.

OTHER SOCIETIES •

NEMA Honored in Trade Association Award Contest

The National Electrical Manufacturers Association was awarded a certificate of honorable mention in the American Trade Association Executives Award Contest recently held for 1942. The certificate was given for the Association's "achievement in carrying out a plan to increase the knowledge and use of all present statistics issued by the various product groups of the Association, and to add any other statistics that might be suggested." The award itself was presented to the Farm Equipment Institute of Chicago, Ill., for an extensive survey of the need for critical materials in its industry. Seven other trade associations, including the American Lighting Equipment Association, also received honorable mention. The contest for recognition of outstanding trade association achievement has been held ten times since its inauguration in 1929. This year's jury of award was headed by Secretary of Commerce Jesse H. Jones as chairman.

ASME Section Honors Alex Dow

Speakers paid tribute to the late Alex Dow (A'93, F'13, HM'37) at a recent meeting of the Detroit (Mich.) section of the American Society of Mechanical Engineers, which was concerned with the shell-turning lathe Doctor Dow helped to originate. This machine for ultrarapid turning of 75- to 155-millimeter shells, was developed by the Ex-Cell-O Corporation, Detroit, and is being produced in great quantities for the war-production program. Its development was first suggested in 1934 by Doctor Dow, who, in addition to being president of the Detroit Edison Company, was for many years chief of the Detroit Ordnance District for the War Department. An obituary notice and biographical sketch of Doctor Dow appeared in the May issue, pages 263–4.

Future Meetings of Other Societies

American Physical Society. 249th meeting, June 25–27, 1942, State College, Pa.; 250th meeting, July 11, 1942, Berkeley, Calif.

American Society for Testing Materials. 45th annual meeting, June 22–26, 1942, Cleveland, Ohio.

American Society of Heating and Ventilating Engineers. Semiannual meeting, June 15–17, 1942, St. Paul, Minn.

American Society of Mechanical Engineers. Semiannual meeting, June 8–10, 1942, Cleveland, Ohio.

Canadian Electrical Association. Annual meeting, June 25–26 (tentative), Murray Bay, Que.

Institute of Radio Engineers. Summer convention, June 29–July 1, 1942, Cleveland, Ohio.

National Fire Protection Association. May 11–15, 1942, Atlantic City, N. J.

Society for Promotion of Engineering Education. Annual meeting, June 27–29, 1942, New York, N. Y.

LETTERS TO THE EDITOR

INSTITUTE members and subscribers are invited to contribute to these columns expressions of opinion dealing with published articles, technical papers, or other subjects of general professional interest. While endeavoring to publish as many letters as possible, Electrical Engineering reserves the right to publish them in whole or in part or to reject them entirely. Statements in letters are expressly understood to be made by the writers; publication here in no wise constitutes endorsement or recognition by the AIEE. All letters submitted for publication should be typewritten, double-spaced, not carbon copies. Any illustrations should be submitted in duplicate, one copy an inked drawing without lettering, the other lettered. Captions should be supplied for all illustrations.

A Simplified Method of Calculating Line-Drop Compensator Settings

To the Editor:

The determination of compensator R and X settings for distribution-feeder voltage regulators by the methods now in use requires either lengthy calculations based on feeder loading and characteristics, or extensive field work. It is believed that the method developed here simplifies the calculations materially and gives results of acceptable accuracy.

The common methods of determining compensator R and X settings now in use are three in number, namely: the analytical method, the trial-and-error method, and the method based on curves determined under high and low power-factor load conditions on the feeder.

The first method involves the following steps:

1. Calculation of feeder resistance and reactance from feeder length, conductor spacing and conductor size, and the line frequency.
2. Calculation of the ohmic line drop and the reactive line drop from the feeder R and X and the maximum actual feeder load current.
3. Expression of the voltage drops in item 2 in per cent of line voltage.
4. Conversion of the percentage drops in item 3 to secondary voltage drops. This conversion involves the ratio of the potential transformer used to provide compensator voltage.
5. Correction of the secondary voltage drops obtained for actual load conditions to rated regulator load current. This gives the required resistance and reactance compensation in compensator dial volts.

The second method involves the establishment of telephonic communication between an observer at the load center and an operator at the station. Load-center voltages are read as the operator sets up all possible combinations of R and X dial settings. Those combinations of R and X settings that give the desired load center voltage are tabulated. It is essential when this method is employed that the procedure be repeated for various load and power-factor conditions. The points common to all tabulations will be those which will give proper compensation for all load and power factor conditions.

The third method, described by B. V. Thor in the *Electric Journal* for December 1930, is fundamentally the same as the second method. Two curves of R setting versus X setting are determined by plotting all combinations which give the desired load-center voltage, first for high power-factor feeder load and then for low power-factor feeder load. The point of intersection of these two curves indicates the correct R and X settings. This method usually cannot be applied because of the difficulty encountered in setting up the required power-factor and load conditions. If load and power factor can be adjusted as required by this method, accurate results will be obtained.

Because of the operating difficulties encountered and the time and cost involved in carrying out either of the two field methods many operating companies have avoided using them whenever possible.

It can be shown that for a given regulator the correct compensator dial settings will be equal to the feeder R and X multiplied by a constant which is determined by the regulator current and voltage ratings. It is possible therefore to set up a table of constants that will apply to all regulator ratings encountered on a given system.

The application of this method, after a table of constants has been set up, involves the following steps:

1. Calculation of feeder resistance and reactance from feeder length, conductor size and conductor Spacing and the line frequency.
2. Selection of proper constant from table.
3. Multiplication of the quantities obtained in item 1 by the proper constant determined in item 2 to give the R and X compensator dial settings in dial volts.

These constants can be developed as shown in the following example. Let us assume a feeder and regulator(s) having the following characteristics:

Single-phase resistance $= R$
Single-phase reactance $= X$
Load current $= I_L$
Nominal phase-to-phase feeder voltage and *nominal rated regulator voltage $= E$

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* In the case of single-phase regulators connected in wye on a three-phase feeder, "E" is the phase-to-phase voltage of the feeder or the rated regulator voltage multiplied by $\sqrt{3}$.

Rated regulator current $= I_R$

Nominal compensator supply voltage $= V_c$

Primary resistance voltage drop $= V_R = I_L R$

Primary reactance voltage drop $= V_X = I_L X$

Primary resistance drop expressed in terms of the compensator supply voltage $= v_R$

Primary reactance drop expressed in terms of the compensator supply voltage $= v_X$

The R drop and the X drop in percentage then will be equal to the primary values divided by the phase-to-neutral voltage or

$$\%\ R\ \text{drop} = \frac{\sqrt{3} I_L R}{E} \tag{1}$$

$$\%\ X\ \text{drop} = \frac{\sqrt{3} I_L X}{E} \tag{2}$$

To express the R drop and the X drop in terms of the compensator supply voltage multiply equations 1 and 2 by V_c or

$$v_R = \%\ R\ \text{drop} \times V_c = \frac{\sqrt{3} I_L R}{E} \times V_c \tag{3}$$

$$v_X = \%\ X\ \text{drop} \times V_c = \frac{\sqrt{3} I_L X}{E} \times V_c \tag{4}$$

Since I_L is not equal to I_R the compensation obtained per compensator dial division is not as indicated on the dial but is equal to I_L/I_R times the indicated compensation. In other words, the required dial setting is inversely proportional to the per cent load, or inversely proportional to I_L/I_R.

Therefore the correct compensation in dial volts will be

"R" setting in dial volts =

$$v_R \times \frac{I_R}{I_L} = \frac{\sqrt{3} I_L R V_c I_R}{I_L E} \tag{5}$$

"X" setting in dial volts =

$$v_x \times \frac{I_R}{I_L} = \frac{\sqrt{3} I_L X V_c I_R}{I_L E} \tag{6}$$

For a given regulator the rated primary current, nominal compensator supply voltage, and nominal phase-to-phase line voltage are all constants. Or

$$\text{"}R\text{" setting in dial volts} = Rk \tag{7}$$

$$\text{"}X\text{" setting in dial volts} = Xk \tag{8}$$

where

$$k = \frac{\sqrt{3} I_R V_c}{E} \tag{9}$$

The development of this constant eliminates the necessity of using two quantities which in some cases are not readily available.

For the particular distribution system on which this method was first applied, the following table of constants was found to cover every regulator now in service.

Nominal Feeder Voltage Phase to Phase	Rated Regulator Current	Nominal Compensator Supply Voltage	Constant K
4,160	200	120	10.00
4,160	200	104	8.66
4,160	250	104	10.83
4,160	400	120	20.00
4,160	400	104	17.32
4,160	500	104	21.65
7,200	100	120	2.89
7,200	200	120	5.78
7,200	400	120	11.55

The constants from this table can be applied to a specific feeder in the following manner:

Length and size of cable: 910 feet of 3× 250,000 circular mil

Length and size of conductor from cable pole to load center: 10,200 feet of number 2/0

Effective spacing:* 53.2 inches

Feeder rating: 3 phase, 60 cycle, 4,160/7,200 volts

Regulator: Three phase step-type regulator rated 200/400 amperes 7,200 volts 10/5 per cent connected for 10 per cent, 200 amperes. Nominal compensator supply voltage = 120 volts

Resistance of 3×250,000 circular mil cable = 0.0437 ohm/1,000 feet

Resistance of number 2/0 = 0.0821 ohm/1,000 feet

Reactance of cable (single phase) = 0.0300 ohm/1,000 feet

Reactance of conductors = 0.1346 ohm/1,000 feet

Single phase resistance of feeder = 0.0437 ×0.91 + 0.0821×10.2 = 0.878 ohm

Single phase reactance of feeder = 0.0300 ×0.91 + 0.1346×10.2 = 1.4002 ohms

The constant for this regulator from the table is 5.78, therefore the R setting is

$$R = 0.878 \times 5.78 = 5.075$$

the X setting is

$$X = 1.4002 \times 5.78 = 8.10$$

The compensator dials should be set for $R=5$, $X=8$.

The R and X settings determined will give correct compensation for straight-line voltage regulation. If overcompensation is desired to provide for voltage drop in distribution transformers and secondaries, the necessary increase may be added to the values of R and X settings previously determined.

In applying this method to a system having 50 regulated feeders it was found that the settings were accurate and that a considerable saving in time over other methods was effected. On this system complete records are available from which it is possible to determine in a very short time the length of feeder from station to load center, size of overhead conductor and underground cable, and spacing of conductors. This information, plus the regulator name-plate data, is sufficient to calculate the compensator R and X settings.

M. W. KECK (A'38), T. J. KOZAK (A'41)

(Engineering department, Toledo Edison Company, Toledo, Ohio)

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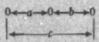
1. The actual potential transformer ratio has no bearing on the development of the constant. The pertinent ratio is that between the phase-to-phase line voltage and the compensator supply voltage. Regardless of the number of transformations from the line voltage to the compensator supply voltage the over-all ratio is the only one that need be considered.

2. The actual feeder loading does not enter into the calculations made to determine compensator settings by this method, so it is not necessary to apply a correction factor (equal to the ratio of regulator rated current to the actual load current) to obtain the final settings. Since the actual load current is not used in the calculation, the compensator settings for a feeder may be determined by this method before the feeder is put in service.

*With conductors spaced as shown the effective spacing is:

Eff. spacing $= \sqrt[3]{a \times b \times c}$



On a standard 7,200-volt feeder on this system $a=29''$, $b=59''$, $c=88''$

Eff. spacing $= \sqrt[3]{29 \times 59 \times 88} = 53.2''$

Bion J. Arnold

To the Editor:

Your obituary item regarding Bion J. Arnold* in the March 1942 issue of *Electrical Engineering* (pages 166–7) fails to do him justice, as it omits all reference to one of his outstanding achievements, viz. his pioneering work in the development of automatic substations. With becoming modesty Mr. Arnold in "Who's Who in America" disposes of the subject in the statement: "inventor of . . . new systems and devices for electric railways."

Mr. Arnold's work along this line is an example of the saying: "Necessity is the mother of invention." He owned a small electric railway line between Elgin and Rockford, Ill., that was not paying expenses. He conceived the idea of remedying the situation by eliminating substation operators. To accomplish his purpose he produced some specifications that contained some features that were so radical that the General Electric Company refrained from bidding. But there were two engineers in their Chicago office who shared Mr. Arnold's enthusiasm on the subject, and they as individuals entered into a contract with Mr. Arnold. Later the contract was taken over by the General Electric Company. I think that this was all set forth by Mr. Arnold in a paper before the Western Society of Engineers, and I distinctly recall being a member of a party from WSE that was escorted to the substation by Mr. Arnold who proceeded to put the automatic devices through their paces.

That the subject of automatic substations is of importance is indicated by the fact that some years later (in 1927) in response to demands of its members, the AIEE appointed a committee on automatic stations, and this committee, with changes in membership, has continued to this day.

D. W. ROPER (F'14)

(Retired electrical engineer, Carmel, Calif.)

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* *Note:* See also commemorative notice in this issue, page 319.

NEW BOOKS • • •

Illuminating Engineering Nomenclature and Photometric Standards. By the Illuminating Engineering Society, New York. 42 pages, paper, 25 cents (quantity prices available).

Intended to supersede the previous IES nomenclature and photometric standards report, the current report, approved by the American Standards Association, aims further to clarify and standardize the nomenclature of the illuminating engineering field. Definitions have been rearranged and renumbered to conform to the system adopted by the ASA.

American Recommended Practice of Industrial Lighting. By the committee on lighting practice of the Illuminating Engineering Society, New York. 51 pages, paper, 25 cents (quantity prices available).

This report, approved by the American Standards Association, presents a discussion of the basic principles of good lighting, emphasizing the correlation between lighting and plant safety. Glare as well as distribution and color of light are considered and minimum standards of illumination are recommended. Natural and artificial lighting and problems relating to maintenance and wiring are also discussed. A bibliography is included.

Standardization Activities of National Technical and Trade Organizations. By Robert A. Martino, National Bureau of Standards. United States Government Printing Office, Washington, D. C. 75 cents.

Intended to provide a complete list of the national technical and trade organizations which carry on standardization activities. The name of the secretary or director and the address of each organization is included with a statement of its standardization activities. The book is available from the Superintendent of Documents, Washington, D. C.

The following new books are among those recently received at the Engineering Societies Library. Unless otherwise specified, books listed have been presented by the publishers. The Institute assumes no responsibility for statements made in the following summaries, information for which is taken from the prefaces of the books in question.

These and thousands of other technical books may be borrowed from the library by mail by AIEE members.

This Chemical Age, the Miracle of Man-Made Materials. By W. Haynes. Alfred A. Knopf, New York, 1942. 385 pages, illustrations, etc., $8^1/_2$ by $5^1/_2$ inches, cloth, $3.50.

The reader without a chemical background will find this an interesting account of modern developments in this field. The ways in which laboratory discoveries have been developed into such industrial products as dies, drugs, plastics, nylon, cellophane, and synthetic rubber are described clearly and dramatically, with scientific accuracy.

Transients in Electric Circuits Using the Heaviside Operational Calculus. By W. B. Coulthard. Pitman Publishing Corporation, New York; Sir Isaac Pitman and Sons, London, 1941. 203 pages, diagrams, 9 by $5^1/_2$ inches, cloth, $8.50.

Dealing especially with electrical engineering problems, this book utilizes the Heaviside operational methods rather than the formal mathematical treatment. The theory of lumped circuits, smooth circuits, and repeated lumped circuits are considered as well as some methods for dealing with variable circuits. A brief bibliography accompanies each chapter.

Thomas' Register of American Manufacturers. 32d edition. Thomas Publishing Company, New York, Boston, Chicago, Cleveland, Detroit, Los Angeles, Philadelphia, Pittsburgh, San Francisco, Toronto, and London, 1941. 5200 pages, illustrated, 14 by 9 inches, cloth, initial subscription $15; renewal $10 (supply limited).

This annual compilation of American manufacturers has its customary three main sections: the classified directory of products (with index) in which the firms are listed, with a capital rating, geographically under each product; the alphabetical list of manufacturers, giving addresses, subsidiaries, branches, etc.; and the trade name index. The newer arbitrary numbering of advertisers, with a separate index which lists these numbers with their corresponding companies is continued chiefly for the use of purchasing agents.

The Science and Practice of Welding. By A. C. Davies. The Macmillan Company, New York; University Press, Cambridge, England, 1941. 436 pages, illustrated, etc., 8 by 5 inches, cloth, $2.25.

Aims to provide a concise, yet comprehensive, account of the basic theoretical principles underlying the various processes of welding and the practical methods of applying them. Both gas and electric methods are covered and there are chapters on gas cutting and on inspection and testing.

Radio Amateur's Handbook. Special defense edition. Published by the American Radio Relay League, West Hartford, Conn., 1942. 288 pages, illustrations, etc., $9^1/_2$ by $6^1/_2$ inches, paper, $1.

The nine basic theoretical chapters of the standard edition have been retained in this edition intended for training courses, and the constructional chapters have been condensed into one which describes representative types of radio equipment. The section on measurements remains, and an introductory chapter covering the necessary elementary mathematics has been added. The material on the construction of amateur equipment and the operation of amateur stations has been omitted.

Possible Alternates for Nickel, Chromium and Chromium-Nickel Constructional Alloy Steels. Contributions to the Metallurgy of Steel. American Iron and Steel Institute, New York, January 1942. 143 pages, charts, etc., 9 by 6 inches, paper, 50 cents.

Presents four new series of alloy steels designed to preserve our reserves of strategic metals, especially chromium and nickel. The steels developed include a series of carbon-molybdenum, manganese-molybdenum, low chromium-molybdenum, and low nickel-chromium-molybdenum steels. Data are given about hardenability and other physical properties.

Power Plant Engineering and Design. By F. T. Morse. Second edition. D. Van Nostrand Company, New York, 1942. 703 pages, illustrations, etc., $9^1/_2$ by 6 inches, cloth, $6.50.

Aims to present a study of electric generating stations, including public service, industrial, and institutional plants. Both mechanical and electrical features and economic factors are included. Steam plants are discussed at length, but hydroelectric and Diesel-engine plants are also considered. A basic knowledge of thermodynamics and mechanics is assumed.

Industrial Electricity, Part 2. (Electrical Engineering Texts.) By C. L. Dawes. Second edition. McGraw-Hill Book Company, New York and London, 1942. 523 pages, illustrations, etc., $8^1/_2$ by $5^1/_2$ inches, cloth, $2.75.

The object of this text is to develop in a simple manner the principles of alternating currents and a-c circuits, and to show their applications to electrical machinery, rectifiers, electron tubes, and to power transmission. The book has been revised, a chapter on rectifiers has been added, and the chapters on illumination and interior wiring have been omitted.

Electrical Circuits and Machinery. Volume Two: Alternating Currents. By F. W. Hehre and G. T. Harness. John Wiley and Sons, New York; Chapman and Hall, London, 1942. 635 pages, illustrations, etc., 9 by 6 inches, cloth, $6.

Intended as a general text for non-electrical engineering students and as an introductory text for electrical engineering students. It is comprehensive in scope and includes two chapters on electronic devices. There are many problems chosen with special reference to present commercial practice.

Technidata Hand Book, Engineering, Chemistry, Physics, Mechanics, Mathematics, etc. By E. L. Page. Norman W. Henley Publishing Company, New York, 1942. 64 pages, diagrams, etc., $8^1/_2$ by $5^1/_2$ inches, looseleaf, paper, $1.00; cloth, $1.50.

Intends to present in condensed form essential data taken from the fields of mathematics, physics, chemistry, and engineering mechanics. Facts, figures, theory, definitions, laws, formulas, simple calculations, diagrams, and numerical tables are given. The use of the slide rule is also briefly described.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1942 Supplement to Electrical Engineering—Transactions Section

Field Tests and Performance of a High-Speed 138-Kv Air-Blast Circuit Breaker

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Synopsis: Since 1926 many significant field tests on high-voltage circuit breakers have been made on the American Gas and Electric Company's central system, which have served as an important aid in high-voltage circuit breaker development. Such tests have also served as a means for studying and checking the behavior of this large power system under short-circuit conditions.

A 138-kv air-blast breaker of novel design has recently been given a series of field tests both for normal interrupting duty and for ultrahigh-speed reclosing service. Circuit interrupting ability at least equal to that expected of any modern oil breaker of conventional design was obtained and, in addition, an unusual superspeed reclosing performance was obtained which points the way to a possible liberalization of existing derating factors for this kind of service.

While still somewhat of an innovation in the high-voltage field, the air-blast circuit breaker undoubtedly has certain basic advantages. It is believed that these tests are an important step forward in developing the possibilities of the air-blast breaker for high-voltage service and that they have brought the entire electric power industry closer to the possibility of benefit from an application of this interrupting principle.

Description of Breaker

At the 1941 winter convention there was presented before the AIEE, a paper[1] describing a new high-voltage air-blast circuit breaker, known as the conserved-pressure type. As built for 138 kv, this breaker has two interrupting units of the axial or longitudinal blast type in series per pole. Each of these units, as shown in Figure 1, consists of an enclosing thick-walled tube of insulating material, containing a stationary and a movable contact, an orifice into which the arc is drawn, and a piston and cylinder for actuating the moving contact. This assembly is housed in a vertical porcelain shell for weather protection. Two such columns plus a disconnecting member comprise a pole unit of the 138-kv breaker. Each pole has a storage tank in which sufficient air for two complete close-open operations is stored at 350 pounds per square inch. The three tanks are coupled together by a common header and connected to a central air compressor plant through a double-acting check valve. Each pole has its own electrically operated blast valve to control the flow of air to the contacts. One pneumatic cylinder with electrically actuated control valves operates the three isolating switches through an enclosed system of push-pull rods between phase units.

The interrupting action of this breaker is unique in that the arc is drawn into a space deliberately maintained at high pressure, instead of into the free air as has been common for other types of air breakers. This back pressure, which is maintained by regulating the size of the vent from the arcing chamber, provides a medium having a dielectric strength several times that of air at atmospheric pressure in which the interrupting contacts are separated.

Referring to Figure 2, the action of the breaker in interrupting a circuit is as follows:

1. The protective relay energizes the coils of the three blast valves, causing them to open, admitting air to the passage leading to the interrupting units.
2. Contacts are separated by action of the pistons in each unit.
3. The arc is drawn into the insulating orifice through which air is passing, where it is extinguished; the moving contact continues on into the area of high pressure and high dielectric strength which prevents the arc from restriking.
4. A definite time after the blast valves have been energized (they are interlocked pneumatically to require action of all three valves) air is admitted to the disconnect actuating cylinder
5. After the disconnect has started to open,

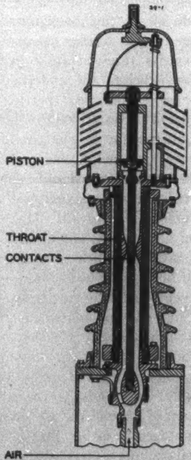


Figure 1. Cross section of interrupting unit

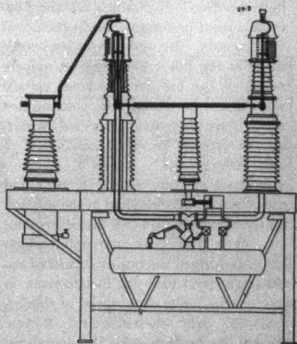


Figure 2. Single-pole unit of 138-kv breaker

Paper 42-9, recommended by the AIEE committee on protective devices for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted July 9, 1941; made available for printing November 3, 1941.

Philip Sporn is vice-president in charge of engineering of the American Gas and Electric Service Corporation, New York, N. Y. H. E. Strang is engineer of the switchgear department of General Electric Company in Philadelphia, Pa.

blast air is cut off, allowing the interrupting contacts to return to the normal closed position after the isolating switch has opened.

The closing action is performed entirely by the isolating switch, its action being fast and controlled by a positive driving force, so that it is capable of closing repeatedly against currents as high as 6,000 to 10,000 rms amperes at 132 kv without harmful effects.

Background of Field-Test Experience

Inasmuch as the facilities of the American Gas and Electric Company's systems, and especially its central system have been lent for the making of interrupting tests on several previous occasions and at the cost of certain extra operating expense and some disturbances to service, it is pertinent at this time, before describing the present series of tests, to review briefly what this background of testing has produced in the way of results and benefits.

While the earliest tests on 138-kv breakers, made in 1926 and 1927[2] produced some real benefits in proving and strengthening the interrupting devices of that time, it remained for later tests, first made in 1930 and later in 1937 and 1938, to bring about important developments in speed. The 1930[3] tests brought to completion the successful development of the 8-cycle breaker, which since has become standard, replacing the 15- to 18-cycle breaker in use up to that time. This improvement in speed was particularly valuable at that time as it permitted taking full advantage of improved high-speed relaying, such as the carrier systems, and in materially reducing the clearing time of faulted transmission circuits. These developments in turn made possible the first attempts at ultrahigh-speed reclosing[4] which has since become a most important tool in the art of transmission. In addition, these tests, made at the full 1,500-megavolt ampere rating of the breaker, disclosed certain weaknesses in design or construction which were thereupon remedied, resulting in greatly increased reliability of breakers in service.

A series of tests, begun in 1937 and completed in 1938[5] resulted in the successful development of a still faster breaker, the 5-cycle multibreak interrupter, which has since formed the basis for most of the ultrahigh-speed reclosing installations on this system. The extra speed of this interrupter over the standard 8-cycle breaker has made possible the overall reclosing cycle, including the time from initial short circuit to final reestablishment of the circuit, of 18 to 20 cycles.[6] This has been accomplished both by the installation of new breakers, as well as by rebuilding existing breakers with the new interrupters.

The 1937–38 tests were further noteworthy in that satisfactory performance was obtained at a duty of 2,000 megavolt amperes, or 33 per cent above the design rating of the breaker. This result has been of real economic value in more ways than one. First, it has formed the basis for a saving in physical size, and consequently in the cost of large 138-kv circuit breakers. Where the 2,500-megavolt ampere breaker was formerly made with a 72-inch tank, with the new interrupter it is now made with a 66-inch tank. Second, it has made possible a rebuilding of a considerable number of existing 66-kv breakers at Windsor for an increased rating well above that for which they were originally designed and at a very substantial saving below the cost of buying new breakers. In this instance, where the normal modernization could offer a rating of not more than 500 megavolt amperes, according to existing standards, a single breaker was rebuilt and successfully withstood a test in the field considerably above this value, and fully adequate for the requirements on these breakers. It is fairly safe to say that without the background of experience gathered in these tests, as well as in the 1930 tests at Philo, the rebuilding and testing of the Windsor breakers for an interrupting duty so much above the maximum previously considered available would never have been initiated.

While all of this background of experience in field testing has proved that the benefits received fully justify the cost, nevertheless, the undertaking of a further series of tests, particularly at this time of increased load requirements, was a matter for serious consideration. For several reasons, however, these tests were believed to be of sufficient importance to justify the actual cost and the inconveniences involved. The development of an outdoor, oilless, high-voltage circuit breaker, which to some may appear to be of only academic interest at the present time, may emerge as a timely, significant, and much needed undertaking. It is not at all inconceivable that restrictions of one form or another may be encountered in the use of oil for future breakers. This leads pertinently to a discussion of some of the advantages and disadvantages of oil as used in conventional oil circuit breakers.

It cannot be denied that oil has proven to be an excellent medium for circuit breakers, and that it has helped to bring them to the present high state of development. Some of the advantages of oil may be listed as follows:

1. Oil possesses high insulating value, or high dielectric strength and uniformity.
2. Oil is a good arc quenching or cooling medium, particularly effective when properly controlled or directed.
3. Behind the use of oil in circuit breakers exists a background of decades of experience and development, culminating in the present successful types of modern breakers.

The disadvantages of oil as a medium for breakers are likewise quite real, and in the course of time, by comparison with air, may appear even greater. Some of these drawbacks are as follows:

1. Oil, being inflammable, constitutes a possible fire hazard. For outdoor breakers, however, this hazard is not regarded as very serious, and experience so far has borne this out.
2. The use of oil presents a maintenance problem of sizable proportions. The problem consists briefly of the following:
   (*a*). The conditioning of the oil itself, including filtering and drying out, and the equipment required for that purpose.
   (*b*). The handling of oil, both for conditioning and for maintenance of the circuit breaker, and the pumping, piping, and storage facilities required.
   (*c*). The longer time and increased cost for breaker maintenance resulting from above oil-handling problems and requirements.

Viewing the comparison from the standpoint of possible future war conditions, oil circuit breakers may present other serious disadvantages. Also, the normal fire hazard from oil circuit breakers may be substantially increased by greater duties accompanying the more rapid growth of systems, and possibly by the necessity for larger physical concentrations or greater crowding of oil circuit breakers.

Object of Tests

While the principal burden of carrying out circuit-breaker development work must of necessity fall on factory high-power testing facilities, nevertheless it is a fact that the best and most satisfying proof of performance of a high-capacity interrupting device comes from tests made on a system large enough to supply at least full rated interrupting current under actual operating conditions.

The value of high-speed reclosing of high-voltage circuit breakers is becoming more widely recognized and the demand is increasing for circuit breakers capable of

Table I. Three-Phase 138-Kv Interrupting Tests

Test No.	Operation	RMS Current (Amperes) Closing	RMS Current (Amperes) Opening	Megavolt-Amperes at 138 Kv	Arc Length (Cycles)	Arc Length (Inches)	Breaker Operating Time (Cycles)
1	O		960	230	0.8	0.6	5.1
2	O		650	155	1.2	1.4	5.1
3	CO	1,040	650	155	2.0	2.5	6.0
4	O		1,480	350	1.6	1.9	5.8
5	CO	2,100	1,270	300	2.3	2.3	6.3
6	O		3,100	740	1.7	1.7	5.3
7	CO	4,300	3,000	720	1.6	1.5	5.3
8	O		5,600	1,340	1.7	1.8	5.6
9	CO	6,900	5,300	1,270	1.7	2.0	6.0
*10	CO	6,200	5,200	1,240	1.5	1.7	5.6
*11	CO	6,000	5,100	1,220	1.3	1.5	4.6
12	O		6,100	1,460	1.6	1.8	5.3
13	CO	8,100	6,100	1,460	1.9	2.1	6.2
14	O		7,700	1,840	1.7	2.1	6.2
15	CO	9,300	7,500	1,800	2.2	2.7	6.0
*16	CO	10,000	7,400	1,770	1.7	2.2	5.4
*17	CO	10,300	7,800	1,860	1.0	1.2	5.2

* Indicates 15-second duty cycle.

performing this duty. In order to find wide application, therefore, any new type of circuit breaker must be capable of clearing a fault promptly, reclosing preferably in less than 20 cycles, and then clearing again in the event the fault on the line still persists.

The circuit breaker field test program, therefore, consisted of two parts:

1. Interrupting tests.
2. Reclosing tests.

The test breaker was rated 138 kv, 1,500 megavolt amperes, 8 cycles with a reclosing time of 20 cycles. All tests were made at approximately 138 kv.

As in the case of both the 1930 tests, and the 1937–1938 tests, these tests were conducted at the Philo plant on the system of The Ohio Power Company. The system setup was substantially the same as that shown in Figure 4 of the reference paper describing the 1937–1938 tests,[5] and for that reason will not be repeated here. As in the case of previous tests, the system connections in so far as possible were so arranged that service areas near the Philo plant would not be directly connected to the short-circuited bus, except on the final, full-capacity shots. Service disturbances were thereby kept to a minimum. A view of the breaker setup for test is shown in Figure 3.

Results of Tests—Breaker

The results of the first series are shown in Table I. Except for the first preliminary test, they were all three phase-to-ground tests, the tabulated values of current and operating time being maximums of the three phases. All tests were made as originally scheduled in a total elapsed time of eight hours, during which there was no inspection of the breaker. Tests 10–11 and 16–17 were made on the standard duty cycle with a 15-second interval. During none of the tests was there any visible fire from vents of the interrupting units. On the closing-opening tests there was a moderate flash from the disconnecting switch contacts, but after ten such operations, of which seven were practically at or above full rating, the blades showed only moderate burning, and so located as not to interfere in any way with their current carrying ability or other normal function.

Following this series of seventeen tests, an inspection of the contacts and interrupting chambers showed them to be in such condition that they were all put back in for the reclosing tests without dressing up or replacement of any parts.

During this first group of tests covering a range of duty from 10 per cent to 125 per cent of the interrupting rating of the breaker, the operating time, measured from energizing the trip coil till the arc was interrupted ranged from 4.6 to 6.3 cycles, including a dead time (to contact separation) of approximately 4 cycles.

It has been shown that in service the minimum reclosing time, without increasing the probability of restrike, is a function of the duration of the original fault, hence of breaker operating time as well as relay time. This gives added impetus to the desire for a fast clearing breaker. Prior to the reclosing tests, it was found possible to make a substantial

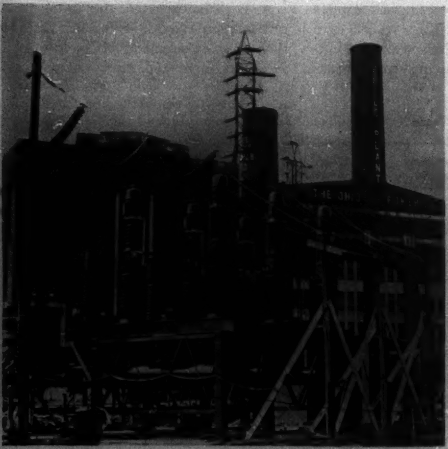
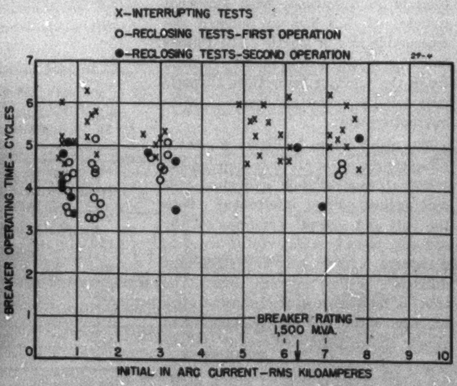


Figure 3 (left). Test setup of 138-kv breaker at the Philo plant

Figure 4 (below). Diagram of breaker performance for both interrupting and reclosing tests



reduction in the breaker dead time by some simple changes and adjustments, thereby reducing the maximum breaker time to approximately five cycles.

The results of the reclosing tests are shown in Table II. During tests 18 to 22 inclusive, difficulties with the temporary control system prevented obtaining complete operation, but did give some interrupting operations showing the effect of the improvement in contact parting time. These control difficulties were remedied, and tests 23 to 25 were completed. In the first operation in each of these tests marked "*a*" in the Table, the fault was initiated by a backup breaker and cleared by the test-breaker interrupting units. The reclosing control was so arranged that instead of the normal sequence of opening the isolating switch next, the blast valve was promptly deenergized, thus cutting off the interrupting air, and allowing the arcing contacts to reclose. This reestablished the fault, causing operation of the protective relays again, opening the blast valves a second time, this time followed by the conventional operation of the isolating switch. After final adjustments, the reclosing time (defined as that measured from energizing the trip coil until the breaker arcing tips contact on reclosing) ranged from 17.7 to 20 cycles. Test 23 represented interruption of light current followed by reclosure on what corresponds practically to normal load current, whereas test 25 demonstrated that the breaker could interrupt a current appreciably beyond its rating, reclose into that fault in less than 20 cycles, and interrupt again with the same operating time for the second operation as for the first.

Following these reclosing tests, the contacts, which had been subjected to a total of 25 operations, were somewhat rounded off, but had not suffered any measurable loss of length. The throats had been enlarged in diameter $^1/_{16}$ inch. As in the previous inspection, all parts were suitable for further operation without replacement.

The chart shown in Figure 4 gives a complete record of all tests showing the operating time of each individual pole unit against current interrupted. Both the first and second operations on the reclosing tests were included in order to determine if there was any significant difference in time between them. The effect of the change in dead time between the interrupting and the reclosing test is clearly shown since the overall breaker time for the reclosing tests ranged from 3.8 to 5.2 cycles compared with a maximum of 6.3 for the previous 17 interrupting tests.

Figure 5 is an oscillogram from closing-opening test 15, interrupting 1,800 megavolt amperes; this may be considered typical of the performance on the first series. Figure 6 shows the results of test 25 in the reclosing series, interrupting 1,770 megavolt amperes, reclosing in 17.7 cycles and then interrupting 1,870 megavolt amperes.

Results of Tests—System

By comparison of the results obtained on these tests, as shown in Tables I and II, with the results obtained on the 1937–1938 tests,[5] it will be seen that the total operating time of the air-blast breaker, particularly in the series of straight interrupting tests, was somewhat longer than the corresponding breaker time of the multiple-break breaker in 1938. For example, on the heavier shots on the oil circuit breaker, breaker time was on the order of 4 cycles, while the duration of short circuit, including relay time, was approximately 5 to $5^1/_2$ cycles. In the air blast tests, however, the actual breaker time for the heavy shots varied between 5 and 6.2 cycles; after adding relay time of approximately 1 cycle, this represented a maximum duration of more than 7 cycles.

As a result of the longer duration of short circuit on these tests, it was to be expected that the disturbing effects on the system would be somewhat greater than those experienced in 1937 and 1938. That this was actually the case was evidenced by a number of reports from nearby areas which would be most affected by the final tests involving a complete system short circuit. These reports were concerned almost entirely with the shutting down of motors having instantaneous undervoltage protection. Certain clay products plants, glass plants and other industrial plants were affected in this manner.

For the reclosing tests, which were begun on June 8 and completed on June 22, the operation of the breaker, as well as the tripping relays themselves, was somewhat speeded up so that the duration of short circuit was more or less comparable to that obtained in the 1937–1938 tests. For this reason, system disturbances were expected to be materially reduced and, in fact, were intended to be practically eliminated by stopping the tests short of the full system capacity and keeping the nearby connected areas entirely isolated from direct connection to the short-circuited bus. This plan, however, was frustrated by a most unusual mechanical failure in the operating arrangements of a three-pole disconnecting switch which inadvertently left the main and reserve busses tied together, thereby subjecting the test breaker and the system to a full short circuit. Since the mechanical failure in the control of this disconnecting switch occurred beyond the motor-operated control mechanism, both the switchboard control light indication and the motor-mechanism check indication showed the switch to be open, whereas the blades of the switch were actually closed and making contact, although not completely driven home. While the test as planned would have subjected the breaker to an ultrahigh-speed reclosing duty at approximately its full rating of 1,500 megavolt amperes, this incident resulted in giving the breaker a reclosing shot at nearly 2,000 megavolt amperes instead.

As is to be expected, the voltage disturbance on the 138-kv system, at the time of short circuit at Philo, tapers off as points more and more remote from

Table II. Three-Phase 138-Kv Reclosing Tests

Test No.	RMS Current (Amperes)		Megavolt-Amperes at 138 Kv.	Arc Length		Breaker Operating Time (Cycles)	Reclosing Time (Cycles)
	Closing	Opening		(Cycles)	(Inches)		
18*a*		1,600	380	0.6	0.1	3.8	
b	2,600	*					21.0
19*a*		1,430	340	1.8	2.0	4.6	
b	2,700	*					21.2
20*a*		1,470	350	2.0	2.6	5.2	
b	2,300	*					23.0
21*a*		790	190	1.9	2.5	4.6	
b	1,160	650	155	1.7	1.9	4.7	21.2
22*a*		3,200	770	2.0	2.4	4.8	
b	**						
23*a*		900	220	1.3	1.2	4.3	
b	1,200	920	220	1.8	2.2	5.1	20.0
24*a*		3,100	740	2.0	2.2	5.1	
b	4,100	3,400	810	1.0	1.1	4.8	18.9
25*a*		7,400	1,770	1.5	2.1	4.6	
b	10,200	7,800	1,870	1.7	2.2	5.2	17.7

* Test breaker did not trip. ** Test breaker did not reclose.

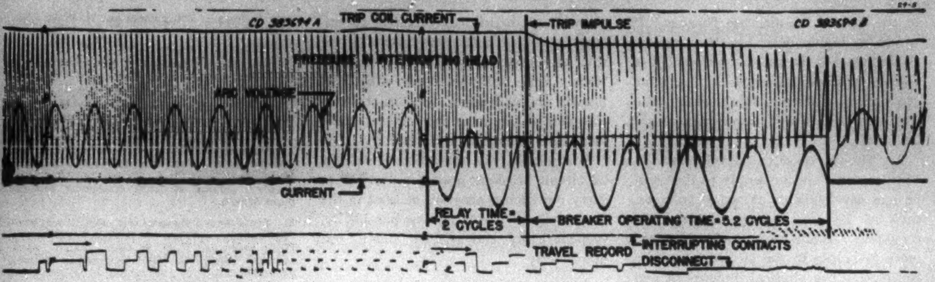


Figure 5. Oscillogram from test 17

Open-close-open operation interrupting 1,860,000 kva

Philo are considered. This was seen from the records taken on the permanently installed automatic oscillographs at Lima, Howard, and Torrey substations. An indication of the relative severity of voltage dips was also obtained from recording voltmeter charts taken at various locations, although the accuracy of these comparisons is questionable because of the variable damping characteristics of the different instruments.

Discussion of Results

As compared with the time which has been spent in developing oil circuit breakers to their present state of satisfactory performance, it is remarkable that in such a comparatively brief time an entirely new principle of arc interruption has been developed and incorporated in a successful breaker which gives substantially five-cycle performance on its first real system test. In other words, while it took thirty years to get the present five-cycle oil circuit breaker, this has been practically realized with air blast in some two years of development work.

This comparison may appear even more striking when it is pointed out that in all previous field tests on oil circuit breakers on this system, it has never been possible to complete an initial series of tests on a new design without disclosing some difficulty in the breaker itself, not of basic importance, it is true, but still sufficient to prevent the completion of the tests until such difficulties were remedied. These tests on the air-blast breaker were, therefore, the first series of tests which was ever undertaken on the American Gas and Electric Company systems in which the breaker in its original condition successfully completed an entire scheduled series without any adjustments, difficulties, or inspections throughout. It is true that on the reclosing tests it was not possible to complete the program as first planned, but this was due entirely to external relay characteristics which had nothing to do with the breaker itself. With proper adjustments in the relay setup, tests were carried through without incident. This is believed in itself to be an unusual performance in the light of past experience with oil circuit breakers and in view of the radically new design of circuit breaker being tested.

Conclusions

1. The high-voltage air-blast breaker tested has demonstrated an interrupting performance at least the equivalent of that expected from any modern oil breaker of conventional design. It may reasonably be expected that present ideas entering into mechanical design and construction will undergo changes during the next few years, but a successful interrupting principle has again been established beyond doubt.

2. With the extension of ultrarapid reclosing applications on the American Gas and Electric Company systems, the adaptability of any high speed breaker to reclosing is obviously an indispensable qualification. The present air-blast breaker has this inherent adaptability, as demonstrated by tests at reclosing speeds at least equal to, and at short-circuit duty far beyond, any similar tests ever made on oil breakers. Based on prevailing standards, the interrupting ratings of 138-kv oil breakers are subject to a reduction of 15 to 25 per cent when applied on 20-cycle reclosing service. Although it may be too early to form definite conclusions, even a conservative interpretation of these test results points the way to a probable downward revision of such derating factors as applied to air-blast breakers in the future.

3. The current-transformer problem is decidedly more complex for the air-blast breaker than for the conventional oil circuit breaker since the relatively simple and economical procedure of applying bushing current transformers to oil circuit breakers cannot be carried out

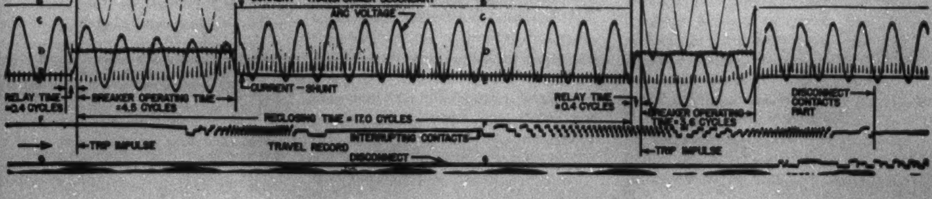


Figure 6. Oscillogram from test 25

Interrupting 1,770,000 kva, reclosing in 17 cycles, and interrupting 1,870,000 kva in the case of the air-blast breaker. It would be highly desirable, therefore, in the interests of the future development of this type of circuit breaker, if a more economical solution of this problem than the separate current transformer could be found.

4. The success of this development on 138 kv points encouragingly to the prospect of developments at higher voltages, such as 230 kv, or even higher. The economic picture here might be even more favorable to the air-blast breaker, considering the physical dimensions and large quantities of oil required in the conventional breakers for such voltages.

5. As regards the possible difficulties in what may be considered an experimental period in the use of air and the equipment required for handling the air, there is no doubt that practical experience is needed. The only way to get this, of course, is through trial installations. This will be done with this breaker and, it is hoped, with others, with the view of acquiring the necessary field experience to handle air successfully.

Fortunately, the existence of reliable oil circuit breakers makes it possible to carry out such a program at least in the moderately high-voltage class like 138 kv, systematically and without delay, but unhurriedly.

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The Acceleration-Oscillogram Method of Motor-Torque Measurement

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Synopsis: The need for an improved method of measuring starting torque of large induction starting synchronous motors as compared to the power input and dynamometer methods, has led to the use of the acceleration oscillogram. A low ripple, permanent magnet type d-c generator is driven by the motor, and traces a time-speed oscillogram, from which acceleration, and the torque producing it, may be calculated. Comparative torque tests, at full and reduced voltage, made by this and the other methods on the dynamometer coupled motor, showed the definite advantage of the acceleration tests in combined convenience and accuracy. Interesting facts about synchronous motor starting torques, not accurately shown by previous tests, have been brought to light. The acceleration or deceleration oscillogram is also applied to the measurement of torques or losses of very small high-speed motors by using other methods of rotation indication, involving no loading of the motor.

THERE has been need in our motor testing departments for a simpler and more accurate means of obtaining speed-torque curves of various types of motors. Particularly this need has been felt in the a-c synchronous motor section, building synchronous motors (induction starting) of from approximately 25 to 1,000 horsepower rating.

Two methods have been used, one based on measurement of power input, and the other on the dynamometer. The first is rather inaccurate because several variable factors can be accounted for only in an arbitrary manner, and the second involves a rather elaborate test setup, including a dynamometer of sufficient torque to load the motor at all speeds.

The method we will describe gives results of accuracy comparable to the dynamometer test. It is taken at full voltage with no corrections, and requires only a simple test setup. It is based on the simple principle that the torque developed in a motor while accelerating from standstill to full speed is absorbed by friction and windage and acceleration of the rotating parts. If only useful output torque is to be determined, friction and windage are not considered, and the required results are obtained from acceleration figures only.

The Tachometer Generator

Speed-acceleration data may be obtained graphically from a time-speed curve, or directly from the proper electric circuit. The first requirement in producing an accurate time-speed curve is a tachometer generator which produces a d-c voltage, free from commutator and rotational ripple, exactly proportional at all times to the rotational speed of the motor. Any ripple in the voltage becomes confused with pulsations from actual speed surges in the starting motor, and makes the resulting curve difficult to analyze.

The use of a satisfactory d-c generator has largely made possible the accurate results we have obtained. The generator is a permanent magnet field type, specially built with a large number of commutator segments for minimum commutator ripple and the best possible armature construction for low rotational ripple. With a 100-microfarad filter condenser, no ripple is visible in the oscillographic trace of the output voltage. The coupling used to connect the generator to the motor must be rigid in torsion, and must not exert any axial or radial force on the generator shaft extension. Any deflection of the generator shaft introduces rotational ripple. The coupling being used is of the common leather ring type, with provision for mounting the motor half by three tapped holes and cap screws on the truly faced end of the motor shaft. Best results were obtained by not clamping the generator too rigidly in position, thus allowing any slight radial deflection to be absorbed by the whole unit, rather than the shaft alone.

The oscillogram method of torque test is especially convenient for the synchronous motor, for other starting data may be obtained on the film. Line current and field discharge current are recorded on the film as well as the speed indicating voltage from the generator.

Torque Calculations From Oscillograms

Calculations from the time-speed oscillogram begin with speed and time calibrations, in terms of revolutions per second per millimeter and seconds per millimeter. The quotient of these two is a constant which may be multiplied by the tangent of the measured slope angle of the speed curve at any point to determine the acceleration. The moment of inertia of the motor and tachometer generator (unless negligible compared to the motor), and normal full load torque may also be included, to give a constant to be multiplied by the slope to give direct values of percentage of full load torque, the term commonly used by motor engineers. (See appendix *A*.) The speed curve is divided into a convenient number of equal sections along the time axis, and the slope and amplitude measured at each section. The results are data for a complete speed-torque curve, percent synchronous speed versus per cent full load torque.

Comparative Tests

A small educational two-unit dynamometer set was chosen for its convenience in illustrating this method of torque test and comparing the results with those of other methods. Both units were the same electrically, rated $7^{1}/_{2}$ horsepower as motors; but one (normally the motor) had a phase-shifting stator mounting, and the other was cradled as a dynamometer.

The input and dynamometer test readings were taken simultaneously over the speed range at reduced voltage (appendix *B*). Starting oscillograms were taken at full and reduced voltage, with the motor coupled and uncoupled to the dynamometer unit. Deceleration oscillograms, coupled and uncoupled, gave data on friction and windage torque at all speeds.

Figures 1 and 2 show starts at full voltage with the dynamometer coupled, from a standstill (1), and with the rotor coasting slowly in reverse rotation before the start (2). The method of Figure 2 permits the transient effect of power application to pass before zero speed is reached. The first, however, gives a better indication of available breakaway torque. This transient starting torque in induction motors has been discussed by Wahl and Kilgore.[1] Figure 3 shows a full voltage start of the motor unit alone, without the added inertia of the dynamometer unit. It can be seen that for accuracy in analysis, when average torque readings are required, the slower start, as obtained with added inertia, is desirable. A reduced

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voltage start, for direct comparison against reduced voltage data from the other methods, is recorded on Figure 4. The tendency of the machine to hunt is shown on the starting curves by the speed pulsations above and below synchronous speed, which did not die out until after the end of the oscillogram.

The speed-torque curve obtained from Figure 4 is plotted on Figure 6, with points added from input and dynamometer tests, all at 140 volts on the 220 volt connection. Agreement is fairly good between acceleration and dynamometer tests but the input method does not show the half-speed dip in torque which is unquestionably present. Figure 7 is for full voltage starting with acceleration torque data from Figure 2 and added points from input and dynamometer tests, with rechecks at some speeds, corrected for 220 volts. Again the dynamometer tests are in best agreement, with input tests not showing the half-speed dip (appendix *C*). Acceleration oscillograms show friction and windage torque (both units) of 4.1 per cent at 600 rpm and 5.8 per cent at 1,200 rpm. These values should be added to the acceleration torque values for exact comparison with input test values.

Variations of the Method

Acceleration may be shown directly on the film by passing the charging current to a condenser (from the d-c tachometer generator) through an oscillograph galvanometer. It can be shown (appendix *D*) that:

Acceleration

$$\frac{d^2r}{dt^2}=\frac{I}{CK_G}+\frac{R}{K_G}\times\frac{dI}{dt}$$

where

I = capacitor charging current
C = capacitance of capacitor
R = total circuit resistance
K_G = volts per revolution per second of generator

The term $I/(CK_G)$ is dependent only on current magnitude and is obtained directly from galvanometer deflection on the film. Correction for the other term would be tedious so it must be kept small by low circuit resistance and low current. The obvious requirement then is a high sensitivity, low resistance galvanometer element.

Figure 5 shows acceleration by this direct method. Unfortunately, the acceleration deflection was not set high enough for accurate reading on smooth acceleration, but shows clearly its value in recording torque pulsations such as those in the synchronous motor approaching synchronous speed. We have found slope measurement of a speed curve more generally useful, because of simplicity of taking the oscillogram, ease of calibration, and the need for average rather than instantaneous torque values.

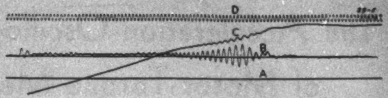

Figure 5. Starting oscillogram with direct acceleration indication (coasting backward before start)

A—Zero speed B—Acceleration C—Speed D—60-cycle timing wave

A speed-torque curve may be traced directly on the screen of a cathode-ray oscilloscope during the starting of a motor by applying the tachometer generator voltage to one pair of plates, and passing the capacitator charging current through deflecting coils (or a shunt and d-c amplifier) to produce torque deflection in quadrature with the speed deflection. This has been done experimentally with good results but has not yet been applied to production torque testing.

The general principle of speed oscillograms for determination of starting, and friction and windage torque has been applied to a number of applications where the tachometer generator could not be used, and speed indication is obtained on the film by other means. Some tests involve high speeds (up to 20,000 rpm) and many do not permit any loading effect by the indicating device. Three methods have been used successfully:

1. Small magnets on the motor shaft generate a voltage in a stationary coil, which is amplified and applied to an oscillograph galvanometer.
2. A mirror on the motor shaft reflects a light directly on the oscillograph film, while a galvanometer traces a timing wave.
3. A mirror or shutter mechanism on the motor shaft controls light on a photocell, and the resulting impulses are amplified and applied to an oscillograph element.

In all of the cases, speed is calculated at any point by counting revolutions per unit time, or measuring time elapsed per revolution.

Conclusions

1. Neither the power input nor dynamometer torque tests are entirely satisfactory in the analysis of synchronous (or induction) motor starting characteristics.
2. The acceleration oscillogram torque test is simpler to set up and run, is taken at full voltage with no corrections, gives consistent, accurate results through the whole speed range, and may be used to determine transient as well as average torque.
3. Variations in the method allow it to be applied to many types and sizes of motors, for determining losses as well as starting torques.

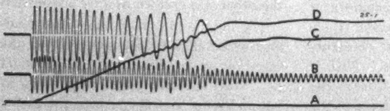

Figure 1. Full-voltage starting with dynamometer coupled (from standstill)

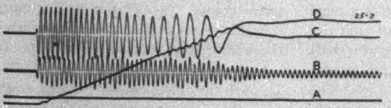

Figure 2. Full-voltage starting with dynamometer coupled (coasting backward before start)

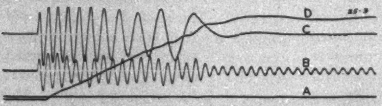

Figure 3. Full-voltage starting without dynamometer coupled (coasting backward before start)

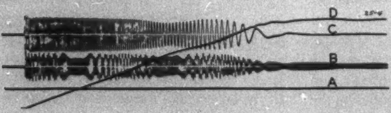

Figure 4. Reduced-voltage starting with dynamometer coupled (coasting backward before start)

A—Zero speed B—Line current C—Field discharger current D—Speed

Appendix A. Derivation of Acceleration Formula

Consider a point on the time-speed curve, H millimeters on the horizontal time scale and V millimeters on the vertical speed scale.

t = time in seconds
r = revolutions
k_h = time calibration, seconds per millimeter
k_v = speed calibration, revolutions per second per millimeter
θ = slope angle of curve
WR^2 = moment of inertia of rotor, pound-feet^2

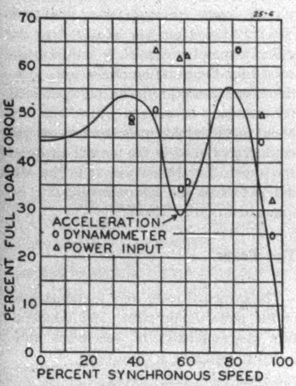

Figure 6. Reduced-voltage (64 per cent) starting torque by various methods

G = acceleration of gravity, feet per second per second

$$\frac{dr}{dt}=VK_v \quad t=HK_h$$

$$\frac{d^2r}{dt^2}=\frac{dKV_v}{dt} \quad dt=dHK_h$$

$$\frac{dr}{dt^2}=\frac{dVK_v}{dHK_h}=\frac{K_v}{K_h}\tan\theta$$ acceleration, revolutions per second per second

$$\text{Torque (pound-feet)}=\frac{WR^2}{G}\times 2\pi\times\frac{d^2r}{dt^2}$$
$$=\frac{WR^2}{G}\times 2\pi\times\frac{K_v}{K_h}\tan\theta$$
$$=K\tan\theta$$

Appendix B. Input and Dynamometer Test Methods

For the input method readings the cradled unit with the dynamometer attachment was connected synchronously to a variable frequency source of sufficient capacity to afford practically infinite bus to the test machine. The motor then was coupled to the dynamometer and connected electrically to a variable-voltage 60-cycle source for short periods during which time the input and torque readings were made. This setup should have provided an ideal arrangement for taking torque tests by either input or dynamometer method, but practically it left much to be desired. Briefly, the range of frequency control was limited, frequency or speed control at very low speeds (including standstill) was not available, and the torque reading on the dynamometer scale varied throughout a reading period, due perhaps to change in the torque characteristics of the armature and amortisseur windings from heating.

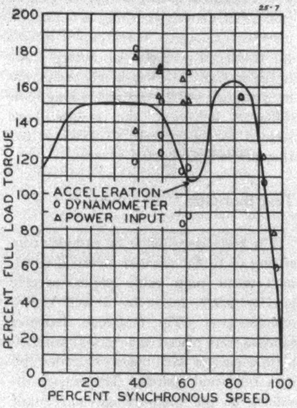

Figure 7. Full-voltage starting torque by various methods

Appendix C. Peculiar Motor Characteristics Shown

The agreement in the lower speed portion of Figure 7 was not good, particularly near half speed, but this was pretty much to be expected. In fact, queer antics of synchronous motors at and near half speed often have been hard to explain in the light of heretofore obtainable tests. In some cases one was inclined to doubt the accuracy of the test readings, in others, the inherent accuracy of the method. We anticipate therefore that this new method of recording torque directly and instantaneously will confirm some previous observations that one hardly dared to believe.

For instance, some motors have shown a tendency to lock or synchronize at half speed (in fact a great many show a slight tendency in this respect). Others seem to show such a characteristic only when being started on a voltage considerably reduced from their rated voltage. A torque test on such a motor by the power input method, at more nearly rated voltage might indicate a peak of torque at half speed. Knowing when to believe such an observation will go far in helping to determine the cause.

Appendix D. Derivation of Acceleration-Current Formula

E_G = tachometer generator voltage (open circuit)
E_C = capacitor voltage
I = capacitor charging current
C = capacitance of capacitor
R = total circuit resistance
K_G = volts per revolution per second of generator

$$E_C=E_G-IR \qquad E_G=K_G\frac{dr}{dt}$$

$$I=\frac{CdE_C}{dt}=C\frac{(dE_G-RdI)}{dt}$$

$$I=\frac{CdE_G}{dt}-CR\frac{dI}{dt}$$

$$I=\frac{CK_G d\frac{dr}{dt}}{dt}-CR\frac{dI}{dt}$$

$$\frac{d^2r}{dt^2}=\frac{I}{CK_G}+\frac{R}{K_G}\times\frac{dI}{dt}=\text{acceleration}$$

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Temperature and Electric Stress in Impregnated-Paper Insulation

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THE effect of temperature elevation on impregnated paper insulation is to increase conductivity and dielectric loss and to accelerate such chemical action as may be possible among the constituent paper, oil, and adjacent electrodes. Both effects increase rapidly above, say 60 degrees centigrade, but are relatively small at ordinary atmospheric temperatures in dry and air-free insulation.

The effect of high stress is also well known. Power-factor-voltage curves in thoroughly impregnated paper (no gaseous ionization) are generally flat up to 300 or 400 volts per mil at temperatures up to 40 or 50 degrees centigrade and at higher stresses rise only slightly until breakdown is approached. Further temperature elevation is immediately reflected not only in large increases in power factor but in characteristic changes[1] in the shape of the power-factor-voltage curve. These immediate (as distinguished from long-time) effects have usually been considered from the standpoint of the values of dielectric loss reached and permissible limits of temperature elevation.

Little attention has been paid to the study of the long-time effect of combined high temperature and high stress. The slow increases in power factor and loss in cables operating at relatively low values of stress and temperature have usually been attributed to chemical changes inherent in the constituent materials and to increasing gaseous ionization. Safe restrictions are placed upon these in design with due reference to the influence of temperature.

However, such time changes have been noted by manufacturers' engineers but, as is so often the case, not published. Time changes in power factor in laboratory samples have been reported by one of the present authors.[1,2] Perhaps the most conspicuous data are those of Proos.[3] In a series of load-cycle studies on power cables he noted the following:

(*a*). Electric stress at low temperature for long time, no permanent change in power factor at high stress.

(*b*). Load (and temperature) cycles plus high stress, large permanent changes in power factor at high stress.

Proos apparently had his eye on gaseous ionization, and says nothing as to possible chemical changes. This paper reports the results of a careful laboratory study of the matter. Although only one oil has been studied so far, and others may show different behavior, the results have seemed to the authors so striking that they are being reported at once.

Test Samples

The tests were made on samples consisting of 10 flat circular layers of 0.004-inch wood pulp cable paper, specific gravity 0.997, made by a well known manufacturer. The 6.25-inch diameter samples were placed in a circular parallel-plate capacitor with brass electrodes and with the usual guards. The outside diameter of the high-voltage and guard electrodes is 5.13 inches. The capacitor was mounted in a Pyrex glass dish so that the impregnated specimen was under oil at all times. The capacitor and dish were mounted in an outer brass cell, with thermostatic control, which could be heated to 105 degrees centigrade and which could be evacuated to a pressure of 0.05 millimeters of mercury. Connections were provided for measuring and for guard electrodes and for voltage up to 15 kv on the high-voltage electrode. A-c measurements were made by a transformer bridge modified to permit rapid determination of power-factor-stress characteristics. Further details of the capacitor and other auxiliary measuring equipment will be found in an earlier paper.[2]

The oil used was prepared by a well known manufacturer for cables of the solid type. The principal properties are given in Table I.

The paper in all cases was vacuum dried and degassed at a pressure of 0.05 millimeter of mercury and a temperature of 105 degrees centigrade for 60 hours, with appropriate measurements of d-c conductivity and 60-cycle power factor at intervals to insure uniformity of final conditions. The oil was stored in a steel drum under nitrogen, and as needed was degassed and dehydrated at 0.15 millimeter mercury.[2] Some of the samples tested were impregnated with oil which had been exposed for 65 hours to oxygen at a pressure of five centimeters mercury, and a temperature of 80 degrees centigrade. The paper for these samples was dried in the usual manner, the oxygen at a pressure of five centimeters was admitted to the impregnating cell, and thereafter the oil saturated with oxygen at the same pressure was admitted. After 12 hours air was admitted to the cell at atmospheric pressure and so maintained for the duration of the test. Under these conditions, the oil contained 0.7 per cent of oxygen by volume, or about five hundred times as much oxygen as that contained in the normal air-dried specimen. Further details as to the oxidation process and measurements may be found in an earlier paper.[2]

The Tests

The principal tests were a comparison of the power-factor-stress and temperature characteristics of specimens subjected to two sets of conditions,

(*a*). Without electric stress, and at a temperature of 80 degrees centigrade.

(*b*). Under an a-c stress of 400 volts per mil, at the same temperature.

These runs lasted approximately one week each, and were interrupted at intervals of approximately 24 hours for the measurement of power-factor-stress relations between 25 and 400 volts per mil. At the end of approximately a week the tests were interrupted and the temperature lowered to that of the atmosphere and another power-factor-stress curve taken. The specimen was then dismantled and power-factor measurements made on individual layers at atmospheric

Table I. Salient Properties of the Oil

Pour point	−18 degrees centigrade
Flash point	279 degrees centigrade
Dielectric strength	30,000 volts per 0.1 inch

	40 Degrees Centigrade	80 Degrees Centigrade
Specific gravity	0.902	0.872
Viscosity	5.1 poises	0.2 poises
20-minute d-c conductivity		8.6 10^{-14} mho per centimeter
Power factor	0.0005	0.0018
Dielectric constant	2.27	2.22

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temperature and a stress of 50 volts per mil.

Results

Test 1. No Stress, No Oxygen, 80 Degrees Centigrade

Figure 1 shows the results of a test run of an air- and oxygen-free sample maintained at 80 degrees centigrade for a period of one week. Time was measured from the termination of the impregnation period. It will be noted that the curves are relatively flat and of low absolute value. The slight increase in power factor at 150 volts per mil over the period of seven days is 0.0005. The curves show the beginnings of a characteristic of all such curves at higher temperatures: namely, a maximum in the neighborhood of the stress mentioned. The power-factor-stress curve, at room temperature and after the test, is absolutely flat. The layer power-factor curve is in no way suggestive of action at the electrodes.

Test 2. Sustained A-C Stress at 400 Volts Per Mil, 80 Degrees Centigrade—No Oxygen

As will be seen in Figure 2 the power-factor-stress curve at the start is approximately flat at a relatively low value and comparable with the initial curve of test 1. However, the curves taken on succeeding days

(*a*). Indicate a rapid increase of power factor with time.

(*b*). Take on the characteristic form indicated in Figure 2.

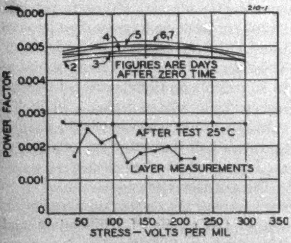

Figure 1. Changes in power factor due to temperature (80 degrees centigrade) alone. No oxygen

The salient features of these curves are the rapid increase in power factor at low stresses, with maximum values at approximately 125 volts per mil, and thereafter the continuous decrease of power factor up to and beyond 400 volts per mil. The increase in the maximum value is approximately linear with time in the lower range, with some indication of a more rapid increase beyond 48 hours. The overall increase in power factor for the period of the run is 0.022, or about 0.0056 per day, which may be compared with the practically negligible increase of test 1 (not greater than 0.00013 per day). The power-factor-stress curve at atmospheric temperature, after the test, is close to the initial curve at 80 degrees and rises slightly with increasing stress, this indicating a permanent change in the dielectric as a result of the run at high stress and 80 degrees centigrade; compare with Figure 1. Again the layer power-factor curve contains no special suggestion of action at the electrodes.

Test 3. Sustained A-C Stress at 400 Volts Per Mil, 80 Degrees Centigrade, No Oxygen

Figure 3 shows three power-factor-stress curves on a specimen similar to that in test 2. Results similar to those of test 2 were obtained in this test which was continued to 112.9 hours. After 112.4 hours at 400 volts per mil, curve *A* was taken. After an additional half-hour at 400 volts per mil, stress was removed, and after a further period of 20 hours without stress curve *B* was taken. After a further 24 hours without stress curve *C* was taken. The three curves of Figure 3 indicate that, while rapid increases in power factor accompany any prolonged application of stress, these increases cease almost immediately upon removal of stress. The high values reached appear to be permanent, indicating a definite deterioration of the insulation. Changes in the power-factor-stress curves at decreasing values of temperature following a run of 91 hours at 80 degrees centigrade and 400 volts per mil are indicated in the curves of Figure 4. As seen, the characteristic maximum in the power-factor curves does not appear below 60 degrees. There is, however, the suggestion that these maxima might appear at 60 degrees centigrade (or lower), if the stress were carried to higher values.

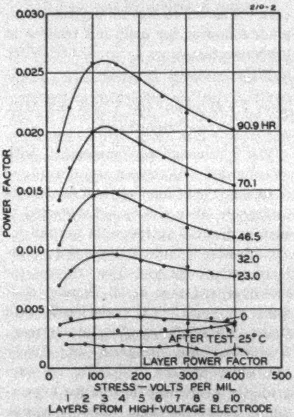

Figure 2. Changes in power factor due to combined temperature (80 degrees centigrade) and stress (400 volts per mil). No oxygen

Test 4. Sustained A-C Stress at 400 Volts per Mil, 60 Degrees Centigrade, No Oxygen

The procedure in this test was identical with that in test 2, except that the sustained temperature was 60 degrees centigrade. The results of the test of 92 hours duration are shown in Figure 5. The trend of the power-factor-voltage curves is similar to that found for the temperature of 60 degrees in Figure 4. Moreover, there is the steady increase of power factor with time at each value of stress. The increase is practically uniform as shown by the graph giving the increasing values at 125 volts per mil. The rate of increase of power factor, 0.000336 per day, is noticeably less than the corresponding figure of 0.0056 per day for temperature 80 degrees centigrade found in test 2. In this case the power-factor-stress curve at room temperature was perfectly flat, at the approximate value 0.003, as com-

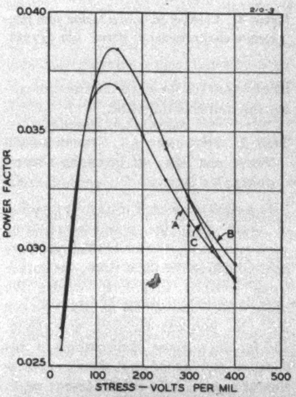

Figure 3. Changes in power factor after removal of stress (80 degrees centigrade). No oxygen

A—112.4 hours under stress
B—112.9 hours under stress, 20 hours off
C—112.9 hours under stress, 43.9 hours off

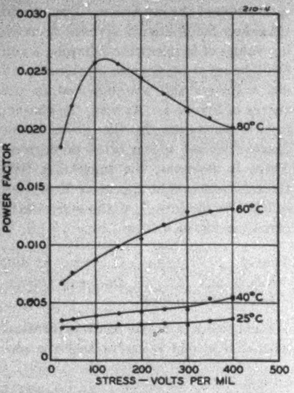

Figure 4. Changes in power factor with temperature after removal of stress. No oxygen

monly observed for all specimens throughout the course of the work.

Test 5. Sustained A-C Stress at 400 Volts per Mil, 40 Degrees Centigrade, No Oxygen

In a similar series of tests over a period of seven days the power-factor-stress curves are all approximately flat and have approximately the same value, the curves being grouped irregularly within the range 0.0027 and 0.0035 at 125 volts per mil. No uniform variation was found, and for the purpose of discussion in this paper, it is assumed that the time variation of power factor in this test is negligible. (0.00011 per day overall average as compared with 0.0056 per day for 80 degrees at 400 volts per mil.)

Test 6. Oxygen, No Stress, 80 Degrees Centigrade

This specimen was impregnated with oil which had been saturated with oxygen at a pressure of five centimeters mercury and a temperature of 80 degrees centigrade for 65 hours; impregnation also took place in an atmosphere of oxygen under the same pressure. The power-factor-stress curves over a period of seven days are shown in Figure 6. The steady rise in power factor (0.0017 overall) is more than three times greater than that of Figure 1 (0.005), which may be attributed to the continuous process of oxidation. There is also a suggestion of the appearance of maxima similar to those in Figure 2. The decrease in power factor between curve "0" day and "1" day is due to the change of atmosphere from oxygen at five centimeters to air at a pressure of 76 centimeters of mercury. The curve for 25 degrees centigrade following the main test remains at low value, but shows an upward tendency at higher stresses.

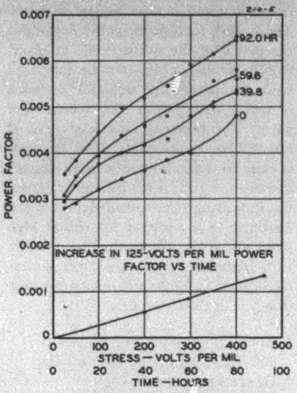

Figure 5. Changes in power factor due to combined temperature (60 degrees centigrade) and stress (400 volts per mil). No oxygen

Test 7. Oxygen, 400 Volts per Mil, 80 Degrees Centigrade

The specimen was impregnated with oil saturated with oxygen at a pressure of 5 centimeters of mercury and 80 degrees centigrade, as in test 6, and subjected to sustained stress at 400 volts per mil for 116.5 hours. Figure 7 shows the power-factor-stress curves. The curves for "0" hour and that at 25 degrees after test, are practically coincident with that of test 6 (oxygen, no stress). Otherwise, the behavior is closely the same as test 2 (stress, no oxygen), the characteristic maxima appearing at about the same values of stress, and the rate of overall time increase being slightly lower than that for the oxygen-free specimen. The time increase of power factor is closely linear in each case, being 0.0056 per day for the oxygen-free specimen and 0.0051 per day for that containing oxygen. The curve for layer variation of power factor again shows no evidence of electrode activity. Figure 8 shows the power-factor-stress curves at decreasing values of temperature following the time-run at 80 degrees. The behavior is closely the same as that shown in Figure 4.

Rate of Power-Factor Increase Versus Temperature—No Oxygen

In Figure 9, based on the data of the foregoing tests, the rate of increase in power factor per hour under sustained stress at 400 volts per mil is plotted as ordinate with temperature as abscissa. As will be seen, an appreciable steady increase is found at 60 degrees, the hourly or daily rate increasing rapidly above that temperature, the increase between 60 degrees and 80 degrees being approximately seventeen times.

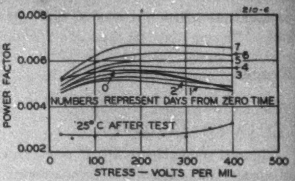

Figure 6. Changes in power factor due to oxidation at 80 degrees centigrade

Discussion of Test Results

The outstanding result of the tests is the very great increase in the time rate of increase of power factor, when high temperature and high stress are applied simultaneously, over the rates observed when the effect of each is observed separately. The relative rates of power-factor increase are: at 80 degrees centigrade and no stress, 0.0005 per week; at 80 degrees centigrade and 400 volts per mil, 0.0392 per week, an increase of at least 78 times in the rate of power-factor increase This figure is startling. Temperatures up to 80 degrees centigrade are not uncommon in high voltage cables. While 400 volts per mil is somewhat high, stresses up to one-half that value are now in use, the higher values are spoken of.

The question immediately arises as to the nature of the underlying phenomena. Within the ranges of temperature and stress here studied, there is a uniform linear rate of increase of loss, hence of conductivity, hence in the number of free ions present. Apparently oxygen plays a relatively small part, an increase of 500 times in the amount of oxygen actually causing a slight reduction in the rate of power-factor increase (which suggests the elimination of possible small active components). We thus fall back on the question of possible interaction among the three constituent materials—paper, oil, and metal.

It has long been known that oil withdrawn from contact with paper and metal electrodes suffers a deterioration as indicated by increased values of power factor and loss. Whitehead and Jones[2] made separate studies of the influence of the contact of oil with paper, with brass, and

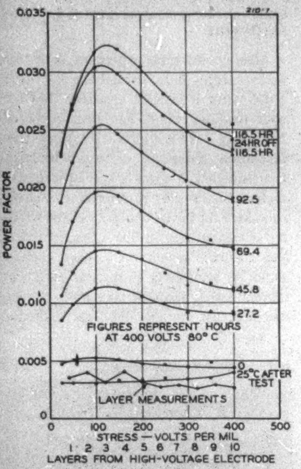

Figure 7. Changes in power factor due to combined influence of stress, temperature, and oxidation. Oil contains 0.7 per cent by volume of oxygen

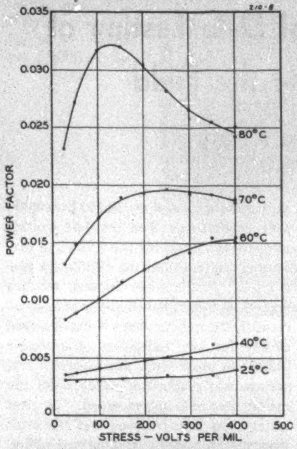

Figure 8. Changes of power factor with temperature after removal of stress. Oil contains 0.7 per cent by volume of oxygen

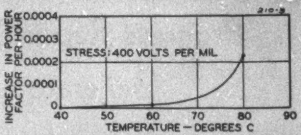

Figure 9. Rates of increase of power factor due to combined influence of stress and temperature

with both, and found that the increase in power factor over the range 20 degrees to 80 degrees centigrade was greater than the changes caused by 0.013 per cent by volume of oxygen in the oil. Thus, even with substantial amount of oxygen in the oil, observed values of the rate of deterioration up to 80 degrees centigrade are entirely accounted for by the contact of the oil with paper and with metal.

It is commonly assumed that there is no inherent electrolytic dissociation in insulating oils. Chemists are very positive in their statement that pure hydrocarbon liquids are free from such dissociation and that insulating oils should also be found in this class. However, it is well known that most such liquids, and certainly the purest insulating oils, have some residual conductivity. This has commonly been attributed to residual electrolytic impurities, but all attempts to identify such impurities, to measure them, and to eliminate them, have failed, and the liquid still shows a residual conductivity. On the other hand, some chemists[4] have maintained that all such liquids should show some electrolytic dissociation and conductivity, albeit of very low values. More recently,[5] studies of the large increase of conductivity of insulating liquids in contact with metals and under high stress have been attributed to large increases in the electrolytic dissociation witl in the liquid.

If an electrolytic dissociation for an insulating oil is admitted, an explanation of the results of this paper seems to be within sight. The powerful influence of temperature in increasing electrolytic dissociation is well known. If to this we add the effect of high stress as a powerful influence for upsetting the stable equilibrium of a complex oil molecule, it is easy to see that in the combined influence of temperature and stress we might find some form of cumulative increase with time of the number of ions present. The deteriorated condition at high temperature appears to remain when the stress is removed. When the temperature is lowered loss and power factor are greatly reduced but the oil does not return to its original condition. This behavior is not inconsistent with the tentative picture presented.

It is a suggestion, therefore, of this paper, that the results reported are due to the fact that the oil itself has, or perhaps acquires as a result of contact with paper and metal, an inherent electrolytic dissociation, and that the rapid changes due to the simultaneous application of temperature and stress are due to the well known influences of these conditions on electrolytic action.

The shape of the power factor–stress curves of Figures 2 and 5 may be explained as follows: increasing stress in the low range is accompanied by increase in the lengths of the excursions of the free ions present in oil spaces, and consequently increases in conductivity and loss. However, as the voltage increases, more and more ions reach the paper barriers until all are active in each cycle, and the loss component tends to a limiting value. The capacitance component of the current increases with stress and ultimately more rapidly than the loss component. Consequently, with increasing stress the power factor passes through a maximum and then begins to decrease as shown. Power factor–stress curves of this shape were reported by one of the authors in an earlier paper,[1] and substantially the same explanation suggested. C. G. Garton and P. Böning have also discussed curves of this type.

It is hoped to continue similar studies with other oils.

Conclusions

1. Chemical instability and deterioration of impregnated paper insulation, due to the combined influence of temperature and electric stress, may begin earlier and increase more rapidly than commonly supposed.

2. Using a high grade insulating oil and a standard cable paper, the time rate of increase of dielectric loss, at 80 degrees centigrade and 400 volts per mil, is over 50 times greater than the rate of increase at 80 degrees without stress, or at 400 volts per mil stress and temperature 40 degrees centigrade. It is recognized that different values may pertain to other oils.

3. Oxygen plays only a relatively small part in the chemical changes in the oil here reported.

4. It is suggested that the behavior reported is due to electrolytic dissociation in the oil, inappreciable at low stresses and temperatures, but increasing rapidly for values which are now being approached in the operation of high-voltage cables.

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Progress Report of D-C Testing of Generators in the Field

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Synopsis: For many years the company with which the authors are associated endeavored to find some satisfactory method of testing generator insulation in the field. With this in mind, it was decided to experiment with higher d-c potentials. Several d-c testers were built and a series of tests made. This paper describes the progress during the first ten years of testing generators in the field with high d-c voltage.

Testing Equipment

3,000-Volt D-C Tester

A 3,000-volt d-c tester was built in 1931 using the simplest of rectifying tube circuits. This tester consisted of two rectifying tubes, filament and plate transformers, and necessary rheostats for voltage control. For purpose of measuring leakage current through armature insulation to ground, a milliammeter was used. A voltmeter was installed for measuring applied potential. Rheostats were installed in the input side of the filament and plate transformers which permitted close control of d-c test voltages from 0 to 3,000.

8,000-Volt D-C Tester

Due to the need for a higher test voltage, an 8,000-volt, one-kilowatt tester, Figure 1, was built in 1934, using practically the same rectifying tube circuit as the 3,000-volt tester. This tester permitted testing with d-c voltages ranging from 0 to 8,000.

14,000-Volt D-C Tester

In order to obtain capacity and voltage believed necessary to test the larger capacity units, a 14,000-volt 2-kw tester was built in 1940, Figures 2, 3, 4, and 5, using a standard bridge rectifying tube circuit.

Test Procedure

3,000-Volt Tests

The first routine tests were made April 6, 1932, on armatures of 3,750-kva 2,300-volt generators and on 18,750-kva 6,600-volt generators. The d-c test voltage applied was 3,000 for the 3,750-kva generators and 2,500 for the 18,750-kva generators. Test voltage between winding and ground was applied and gradually increased, the milliammeter being observed closely for any indication of excessive leakage or breakdown, until specified test voltage was reached at which point the current was read and recorded. The test voltage was then removed and generator armature grounded to discharge winding. If, however, during the initial application of test voltage, evidence of undue leakage or breakdown was noticed, test voltage was immediately removed and reapplied, slowly increasing voltage to determine the point at which this indication was observed.

8,000-Volt Tests

A new series of tests was instituted June 12, 1934, using same procedure and recording milliampere readings as described before. The d-c test potential applied on these tests was as follows:

3,000 volts for generators rated at 2,300 volts.

8,000 volts for generators rated at 6,600 and 13,000 volts.

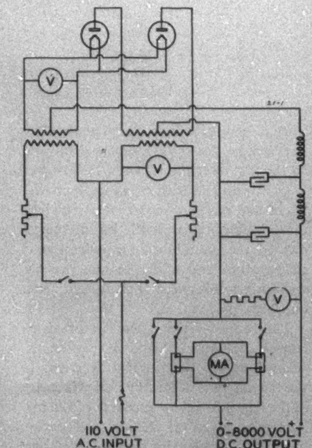

Figure 1. Schematic diagram of high-voltage d-c tester

After considerable experience in testing and recording data, using both 3,000 and 8,000 volts, it became apparent that the spot readings which had been recorded heretofore were not indicative of the true insulation resistance or characteristics. Consequently, a series of special tests was undertaken in order to determine the time necessary for initial charging of the winding at various temperatures (Figure 6) before the true insulation leakage current could be accurately observed. D-c potential was gradually increased to the specified test voltage and then held constant continuously on the armature for remainder of test with a current reading recorded at the end of the first minute of the specified test voltage application and continuing with each minute of test. The specified test voltage applied for this series of tests was 3,000 for the generators rated at 2,300 volts and 8,000 for the generators rated above 2,300 volts. In addition to current readings, data on relative humidity, barometric pressure, and core iron temperature were also recorded. The generator winding was grounded prior to beginning of tests and also between each five- or ten-minute test of a series.

Figure 2. Testing instruments showing carrying case

From experience it was found that high per cent relative humidity during test periods has no bearing upon the test readings provided the temperature of the generator under test is higher than ambient. However, if any outdoor bus is included in the test, high per cent relative humidity will cause the milliampere readings to increase sharply, thus giving false indications of insulation leakage or resistance.

The age of insulation appears to have no effect upon the magnitude of milliampere test readings except in a new machine or new windings in old machines. It is evident that the new insulation goes through an aging process and becomes dryer. This aging condition is illustrated by curves, Figure 8.

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Figure 4. Field testing of an 18,750-kva 6,600-volt generator

Results

3,000-VOLT D-C TESTS

During the course of the first routine testing, one breakdown occurred on a 3,750-kva, 2,300-volt bar-wound generator at a d-c test potential of 2,500 volts. On inspection of the point of breakdown, it was apparent from the condition of the surrounding insulation that leakage had been occurring at this point for some time. Eventually, the winding would have failed at this point with probable severe damage to core iron and adjacent coils. One new armature bar was installed in repairing the generator.

An 18,750-kva 6,600-volt lap-wound generator broke down on d-c test at 1,500 volts. The generator was then tested with a conventional tester which showed the insulation resistance to ground to be 150 megohms. The insulation of this winding consists of partly mica and partly varnished cambric which is more commonly known as "composite" insulation. Upon close inspection of the winding, a broken piece of core iron was found to have cut into a coil. The probable reason breakdown did not occur while generator was in service was that this coil was located near grounded neutral. Six new coils were installed in repairing the generator.

8,000-VOLT D-C TESTS

The use of the higher d-c test voltage proved beneficial in that several cases of excessive leakage were observed on routine test, which would not have been noticeable had the lower test voltage been used. In one case, the milliammeter read off-scale, 100 plus at 4,000 volts on routine d-c test of an 18,750-kva 6,600-volt lap-wound generator, when for this generator the current, as indicated from test records, should have been approximately 0.12 milliampere at 8,000 volts. In this generator the insulation in the slot portion is mica and the coil ends are of class *A* (fabric). This reading immediately indicated serious leakage to ground. The phases were separated and leakage located in slot portion of a coil midway between top and bottom of core iron. Upon examination of coils when generator was dismantled, it was found that at some time a nut had dropped down between the rotor and stator while generator was in service, cutting into core iron and coil insulation in the front of the coils (Figure 7) but not quite through to the copper. Four new coils were installed in repairing the generator.

In another case, on 8,000-volt d-c test of an 8,000-kva 11,000-volt turbogenerator, the current reading was 8.5 milliamperes which began climbing steadily after about 15 seconds from beginning of test and had increased to 16.0 milliamperes within one minute; therefore, the test voltage was removed. The test voltage was reduced to 4,000 and the current reading remained constant at 3.7 milliamperes for the entire ten-minute period of test voltage application. Next, the test voltage was increased to 6,600 at which the initial current was 6.5 milliamperes but began to rise rapidly as before, so test voltage was removed. Finally, the test voltage was slowly increased from zero in order to determine at what applied d-c voltage the current to ground began to increase, and this was found to be at 6,500 volts. The generator lead cables were disconnected and the generator was again tested with breakdown occurring at 3,500 volts. The end connectors were removed and the generator tested at 8,000 volts with the current varying from 2.64 to 2.5 milliamperes at 58 degrees centigrade within the standard ten-minute test period. Upon examination of the end connectors there was evidence of slight arcing

Table I

Year	Armature Tests		Total No. Tests	Generators Tested		Generators Failing on Test	
	8,000 Volts	3,000 Volts		Number	Kilovolt-Amperes	Number	Kilovolt-Amperes
1932	—	117	117	75	934,805	3	11,250
1933	—	121	121	77	942,730	1	3,750
1934	125	166	291	80	943,855	3	41,250
1935	90	65	153	87	904,834	1	294
1936	131	85	216	87	904,834	2	3,000
1937	187	112	299	86	927,084	2	7,190
1938	226	154	380	90	1,014,083	1	12,500
1939	191	90	281	87	1,012,834	2	26,750
1940	218	116	334	90	1,028,334	0	0
1941	146	73	219	88	1,131,834	3	26,250
Total	1,314	1,097	2.411				

Figure 3. Portable 14,000-volt rectifier with cover removed

Table II

Year	Generator		Standard D-C Test Volts	D-C Test Volts Breakdown Occurred	Dangerous Condition Indicated	Nature of Trouble or Breakdown	Repairs Required
	Kva	Kv					
1932	3,750	2.3	3,000	2,500	—	Bar failure	1—new bar
	3,750	2.3	3,000	2,500	—	Bar failure	4—new bars
	3,750	2.3	3,000	2,500	—	Bar failure	1—new bar
	3,750	2.3	3,000	—	Rising current	Cable ground	New section
1933	3,750	2.3	3,000	2,500	—	Bar failure	1—new bar
1934	3,750	2.3	3,000	3,000	—	Bar failure	1—new bar
	3,440	2.3	3,000	—	Rising current	Cable ground	New section
	18,750	6.6	8,000	3,000	—	Coil failure	6—new coils
	18,750	6.6	8,000	3,000	—	Coil failure	4—new coils
1935	294	2.3	3,000	3,000	—	Coil failure	10—new coils
1936	1,500	2.3	3,000	1,000	—	Coil grounded	1—new section
	1,500	2.3	3,000	3,000	—	Coil grounded	12—new coils
1937	3,440	2.3	3,000	2,000	—	Coil failure	1—new section
	3,750	2.3	3,000	1,000	—	Coil grounded	1—new bar
	7,800	6.6	8,000	—	Rising current	Cracked insulators	Replaced
1938	12,500	6.6	8,000	3,500	—	Coil failure	1/2 winding
	7,800	6.6	8,000	—	Rising current	Wet cables	New section
1939	18,750	6.6	8,000	—	Rising current	Mechanical	4—new coils
	8,000	11.0	8,000	—	Rising current	End connectors	Reinsulated
1940	3,750	2.3	3,000	—	Rising current	Cable ground	1—new section
1941	3,750	2.3	3,000	300	—	Bar failure	1—new bar
	3,750	2.3	3,000	3,000	—	Bar failure	1/2 winding
	3,750	2.3	3,000	—	Rising current	Cable ground	1—new section
	18,750	6.6	8,000	1,000	—	Coil failure	1—Coil cut out

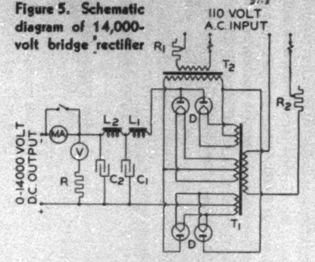

Figure 5. Schematic diagram of 14,000-volt bridge rectifier

T_2—110/20,000-volt transformer
T_1—110/2.5-volt transformer
R—500,000-ohm resistor
R_1—Rheostat R_2—Rheostat
C_1—Capacitance—0.5 microfarad
C_2—Capacitance—1.0 microfarad
L_1—Choke—12–38 henries
L_2—Choke—20 henries
V—Voltmeter
MA—Milliammeter
D—Rectifier tube—type 866A

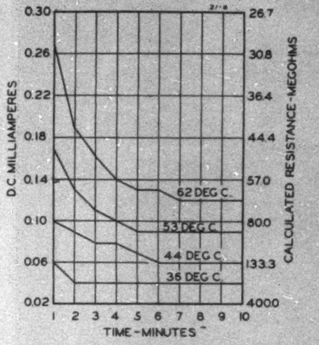

Figure 6. Variation of leakage current with coil temperature

Temperature obtained with imbedded detectors

Table III

Generator			Periodic Test Readings																						D-C Test Volts
			1934		1935		1936		1937		1938				1939				1940				1941		
Kva	Kv		1	2	1	2	1	2	1	2	1	2	3	4	1	2	3	4	1	2	3	4	1	2	
43,750	13.2	Milliamps.	0.06	0.08	0.85	0.40	0.06	0.10	0.04	0.05	0.10	0.10	0.52	0.34	0.46	0.55	—	—	0.36	0.48	0.22	0.09	0.15	0.14	8,000
		Temp. °C.	30	27	35	23	48	39	25	27	17	17	15	18	20	52	—	—	45	38	38	45	36	35	
18,750	6.6	Milliamps.	0.90	1.0	1.4	0.10	0.07	0.08	0.10	0.06	0.06	0.51	0.28	0.03	0.28	0.38	0.38	0.02	0.08	0.70	0.07	0.06	0.06	0.10	8,000
		Temp. °C.	45	65	47	27	48	54	50	38	48	35	48	24	48	50	52	22	38	44	30	38	40	40	
22,500	6.6	Milliamps.	0.25	0.20	0.26	1.1	0.15	0.06	0.06	0.08	0.03	0.80	0.11	0.04	0.03	0.34	0.20	0.03	0.09	0.46	0.10	0.03	0.02	0.17	8,000
		Temp. °C.	45	60	58	71	36	42	44	48	40	52	48	24	39	29	36	32	44	58	46	32	26	42	
12,500	6.6	Milliamps.	0.60	0.30	0.24	0.60	0.10	0.06	0.08	0.04	0.06	0.03	0.29	0.07	0.15	0.38	0.35	*	0.09	0.18	0.22	0.04	0.02	0.24	8,000
		Temp. °C.	60	65	61	69	55	53	58	50	40	42	54	46	55	52	48		23	30	42	26	24	50	
18,750	6.6	Milliamps.	0.68	1.5	1.6	1.6	1.4	0.02	0.13	0.10	0.13	1.5	0.60	0.10	0.21	0.36	0.64	0.12	0.18	0.78	0.17	0.14	0.05	0.25	8,000
		Temp. °C.	43	65	62	50	54	28	49	42	56	60	60	36	48	42	58	42	52	50	36	47	36	53	
10,625	6.6	Milliamps.	0.40	0.30	0.18	0.40	0.06	0.04	0.04	0.05	0.03	0.62	0.18	0.06	0.07	0.43	0.78	0.06	0.01	0.24	0.10	0.08	0.02	0.18	8,000
		Temp. °C.	49	51	51	32	28	42	46	42	41	24	34	42	40	48	48	42	26	30	26	44	26	40	
18,750	13.2	Milliamps.	0.64	0.64	0.10	0.89	0.02	0.06	0.01	0.05	0.00	0.02	0.30	0.05	0.01	0.04	0.16	0.08	0.02	0.14	0.06	0.11	0.02	0.03	8,000
		Temp. °C.	29	33	17	34	43	28	30	37	8	15	36	19	10	18	38	47	27	36	28	22	20	27	
3,440	2.3	Milliamps.	0.10	0.06	0.04	0.14	0.01	0.01	0.03	0.04	0.01	0.02	0.07	0.05	0.09	0.02	—	0.12	0.01	0.02	0.06	0.05	0.02	0.07	3,000
		Temp. °C.	50	40	18	21	56	49	61	60	42	20	25	18	54	50	—	67	44	50	48	58	50	66	
14,000	6.6	Milliamps.	1.0	0.65	0.52	0.80	0.04	0.09	0.10	0.09	0.09	1.2	0.91	0.54	0.24	0.46	0.88	0.32	0.34	0.44	0.40	0.31	0.28	0.35	8,000
		Temp. °C.	70	64	59	65	18	78	78	73	58	80	80	76	32	30	72	20	24	42	40	24	24	32	
3,750	2.3	Milliamps.	0.17	0.40	0.02	0.20	0.02	0.04	0.13	0.03	0.01	0.39	0.14	0.05	0.09	0.29	0.34	0.01	0.02	0.07	0.16	0.02	0.01	0.30	3,000
		Temp. °C.	58	60	26	76	32	74	29	15	24	30	24	30	19	35	34	12	45	25	28	13	11	71	
7,500	6.6	Milliamps.	0.64	0.70	0.60	2.6	0.04	0.01	0.24	0.06	0.06	0.50	0.20	0.08	0.16	0.26	0.66	0.01	0.01	0.20	0.06	0.02	0.02	0.05	8,000
		Temp. °C.	50	55	51	67	38	52	48	32	44	34	50	38	48	50	64	8	12	32	26	12	7	30	

* Generator failed.

Figure 7. Mechanical damage to a large generator which was located with d-c tester

from one end connector to metal supporting bracket. The end connectors were repaired and replaced and final test at the end of the ten-minute test period at 8,000 volts showed a current leakage of 0.84 milliampere at 23 degrees centigrade.

14,000-Volt D-C Tests

Up to the time this paper was written, no higher d-c test voltage than 8,000 has been used. However, all d-c tests since 1940 have been made using the 14,000-volt test apparatus. It is planned at some later date to make tests on the 13,200-volt machines at nearer rated voltage than has been done before. The results of these tests when they are performed are merely a matter of conjecture, but from previous experience in testing, it is believed that the higher d-c voltage will prove more satisfactory than that used at present on the higher-voltage machines.

Initial High Charging Current

The curves shown in Figure 8 illustrate initial high charging current with the current gradually decreasing to a constant value and remaining constant for several minutes toward the end of the standard ten-minute test period. These curves are quite interesting because they were taken from the time the generator was new, June 28, 1938, up to January 3, 1941. Curves 1, 2, and 3 show the average of periodic tests, and, from a study of these curves, it is evident that the insulation leakage current has made a noticeable decrease over an operating period of approximately two and one-half years. The foregoing curves are of a generator rated at 68,750 kva 13,200 volts, having an average loading factor during this period of 95 per cent. The insulation of this generator is continuous mica tape, ASA classification *B*.

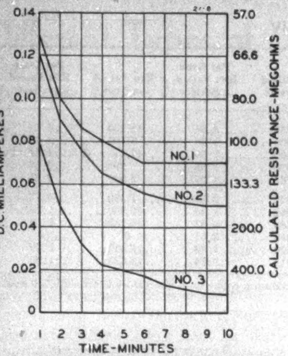

Figure 8. Periodic tests showing insulation characteristics of a 68,750-kva 13,200-volt generator armature

Number 1—June 28, 1938—46.8 degrees centigrade coil temperature

Number 2—March 2, 1940—48.9 degrees centigrade coil temperature

Number 3—January 3, 1941—50.0 degrees centigrade coil temperature

In all tests represented by curves shown, an attempt was made to duplicate as nearly as possible the temperature conditions of previous tests.

Defective Auxiliary Apparatus

Defective apparatus was found on test such as metering and synchronizing potential transformers, cracked bus insulators, and, in one case, an insect's mud nest on bus insulators caused high milliampere readings. In another case, a defective 13,000-volt potential transformer was found on a routine d-c test at 8,000 volts. The milliampere readings were found to increase steadily with each minute's duration of test voltage application. The curves shown in Figure 9 illustrate this condition as compared with previous test on this particular generator. This increase immediately indicated trouble because, as has been shown previously, the current is higher at the beginning of the test, dropping to a constant value as the test period continues. The potential transformer fuses were removed and the current immediately dropped to 0.10, showing definitely that the excessive leakage was due to the defective transformer.

Test Summary

A summary of the d-c testing in the field by the company with which the authors are associated is shown in Table I, in which the total number of tests and failures is listed as compared with number of generators tested.

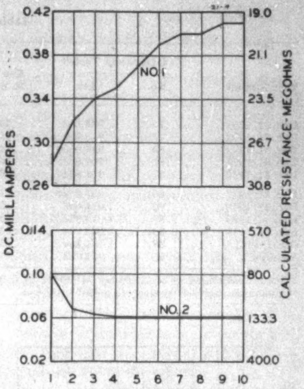

Figure 9. Characteristics of defective apparatus

Curve 1— Before removing transformer fuses; excessive leakage indicated by increasing current

Curve 2—After removing transformer fuses; normal leakage indicated by decreasing current

A detailed listing of generator failures and some miscellaneous troubles found as a result of periodic high-voltage d-c testing is shown in Table II.

The method of correlating periodic tests on individual generators by a comparison of milliampere readings and temperatures is given in Table III.

The considerable variation between some of the periodic readings can be attributed in most cases to long periods of complete shutdown. When any abnormal increase in milliampere readings is found, further investigation is carried on by testing each phase separately to ground. During this test the remainder of the winding is grounded. This individual phase test has recently been incorporated as an annual test along with the regular periodic test schedule.

A comparison of generator failures while in service and on d-c test is shown in Table IV.

Conclusions

In a series of periodic readings on any one generator at approximately the same temperature with identical test voltage applied, any change in magnitude of milliampere readings denotes a change in the insulation characteristics. In order to determine the normal leakage or to detect changes in leakage current, accurate records must be kept of tests on each generator so that a comparison of periodic readings may be made.

Table IV

Year Ending	Generators Tested No.	Generators Tested Kva	Kva Failed in Service	Per Cent Kva Failed in Service	Kva Failed on Test	Per Cent Kva Failed on Test
Dec. 31, 1932	75	934,805	32,750	3.51	11,250	1.21
Dec. 31, 1933	77	942,730	64,900	6.88	3,750	0.40
Dec. 31, 1934	80	943,855	34,500	3.66	41,250	4.37
Dec. 31, 1935	87	904,834	35,125	3.88	294	0.03
Dec. 31, 1936	87	904,834	36,769	4.07	3,000	0.33
Dec. 31, 1937	86	927,084	21,975	2.36	7,190	0.77
Dec. 31, 1938	90	1,014,083	42,062	4.15	12,500	1.23
Dec. 31, 1939	87	1,012,834	49,384	4.88	26,750	2.64
Dec. 31, 1940	90	1,028,334	26,850	2.61	0	0
Oct. 1, 1941	88	1,131,834	15,800	1.39	26,250	2.32
Average	85	974,523	36,012	3.74	13,223	1.33

Averages calculated as of December 31, 1941.
Minimum capacity of generators tested—250 kva.
Maximum capacity of generators tested—68,750 kva.

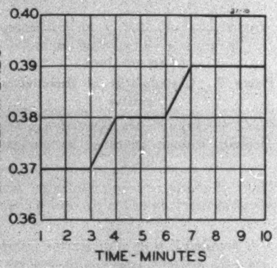

Figure 10. Characteristics of defective insulation

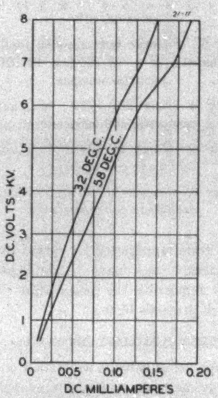

Figure 11. The relation between leakage current and applied voltage

Figure 12. Defective section of coil insulation of a 12,500-kva 6,600-volt generator located with d-c tester

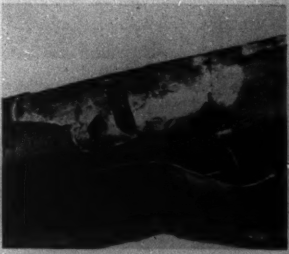

Figure 13. Defective section of coil insulation of an 18,750-kva 6,600-volt generator located with d-c tester

It was found that the 3,000-volt d-c test voltage will give excellent results in testing generators rated at 2,300 volts, 3,750 kva and below. No definite conclusions have been made as to the proper test voltage to be used for testing larger-capacity generators rated at 6,600 volts and above, although 8,000 volts has given very good results so far.

From a series of special tests it was found that the length of time required for the current to reach a constant value which is taken as the criterion of insulation resistance varied with the size, voltage, and coil insulation of the generators. Thus, the following conclusions for time of application of test potential were reached:

Five minutes at 3,000 volts for generators rated up to and including 3,750 kva, 2,300 volts

Five minutes at 8,000 volts for generators rated up to and including 18,750 kva, 6,600 volts

Ten minutes at 8,000 volts for generators rated up to and including 68,750 kva, 13,200 volts

From a study of curves (Figure 11) it may readily be seen that more accurate readings can be obtained at the higher d-c voltages. This permits using a rugged, portable milliammeter which increases the practical value of the testing unit. The values given in these curves were taken on a 22,500-kva 6,600-volt hydrogenerator which was out of service for repairs and was kept heated with space heaters so that no change in temperature occurred between readings. The generator winding was grounded for ten minutes before each voltage test. Each test voltage was applied for the standard ten-minute test period with a current reading taken each minute. The final reading at the end of the test period under each test voltage was plotted so that these curves show no initial charging current to the winding.

It has been the ultimate goal in this testing to detect, if possible, insulation weaknesses before actual failure occurred; therefore, higher d-c voltages have decided and definite possibilities in generator testing because actual breakdown of incipient insulation weaknesses was accomplished which had hitherto not been done with conventional testers. A definite indication of subsequent failure of generator insulation is denoted by a steady or sudden increase in magnitude of milliampere test readings during the test period (Figure 10). This condition, when found, demands immediate investigation. These latent weak spots were found without serious damage to adjacent coils and core iron, which would have occurred if the generator had been allowed to fail in service.

From our experience in d-c generator testing, considerable knowledge has been gained concerning the causes of generator failures and it is believed that the beginning of a majority of generator failures may be traced directly to some mechanical defect.

The testing of generators and the location of troubles before failure in service with the higher d-c voltages over a period of ten years has given us excellent and economical results.

Reference

1. Insulation Resistance of Armature Windings, R. W. Wieseman. AIEE Transactions, volume 53, 1934 (June section), pages 1010–21.

Relative Value of Different Types of Overcurrent Protection for Distribution Circuits

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ASSOCIATE AIEE

LINE sectionalizing in the main feeder and individual protection at branch-line junctions offers a practical solution to the problem of service interruption caused by overcurrent faults on distribution lines. Actual operating data have shown that as high as 96 per cent of all consumer minutes outage (minutes of outage per consumer per year) are caused by faults out beyond the substation, with 85 to 90 per cent of the outage time resulting from faults occurring on the primary lines. These faults are divided between those which persist so as to require servicing by a line crew (permanent faults) and those which are temporary, so that they cause a negligible momentary opening of the circuit or a momentary collapse of the voltage.[1] Operating experience shows that the percentage of temporary faults varies from 15 to 85 per cent of the total,[2] depending on local conditions and the amount spent on such things as tree trimming and pulling up slack.

The judicious use of overcurrent protective relays and breakers, reclosers, and fuse cutouts depends upon a knowledge of the function and characteristics of these devices[1] and the relative benefit each affords. Automatic opening and reclosing of the circuit can be accomplished with all three types. The relay and some types of reclosers reset automatically after a temporary fault, whereas other types of reclosers and reclosing fuse cutouts require manual resetting or the installation of a new fuse link after a prescribed number of operations. Operating records (Table I) show that the first reclosing of the circuit is the most effective in service restoration, tapering down substantially for subsequent reclosures.[3]

Successful line sectionalizing and branch protection depend on the adequacy of available equipment to meet operating requirements. Advancements in the design and manufacture of fuse links for distribution cutouts have made them approach the high degree of accuracy and dependability of the induction relay.[4] Service records of selectivity of operation over four or five years are being secured with fuse links connected in series and in series with relays, revealing no improper operations.

The major problem is, therefore, when and how best to apply these different devices to provide the best service continuity that can be justified economically. There is a real need for actual operating data evaluating the benefits numerically. This would provide information on the devices under the differing conditions of the various systems. It would not, however, give a comparison of the relative benefits of the different devices under identical conditions. It seems there is little prospect of an early investigation being conducted in service to determine this relationship, so a mathematical study has been substituted. Naturally, this study is limited by the very rigidity of the assumptions, even though they are made as closely as we know how, to operating experience. However, we believe the study should provide a greater knowledge of the relative improvements in service continuity provided by available equipment. This will aid in the selection and application to suit the needs and economics of a particular circuit.

Calculations and Presentation of Data

Separate studies were made on two different setups of the distribution lines, namely: one for branch protection, as in Figure 1, and another for line sectionalizing with protective devices connected in series, as in Figure 2. Using the assump-

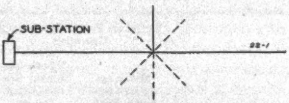

Figure 1. Individual branch line protection added to 30-mile feeder

Branches located at center of feeder for average of even spacing along feeder

No. Branches	Total Length of Line 1-Mile Branches	5-Mile Branches	10-Mile Branches
0	30	30	30
1	31	35	40
2	32	40	50
3	33	45	60
4	34	50	70
5	35	55	80
6	36	60	90

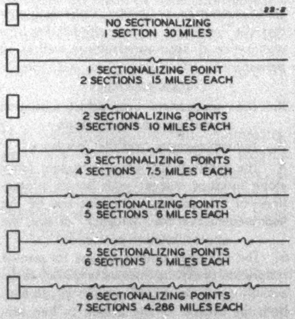

Figure 2. Line sectionalizing with protective devices connected in series on 30-mile line

Table I. Distribution-Feeder Oil-Circuit-Breaker Operation Analysis*

Breaker Held on	1935 No. of Faults	% of Total	1936 No. of Faults	% of Total	1937 No. of Faults	% of Total	Three-Yr Total No. of Faults	% of Total
1st reclosure	100	61.3	96	57.5	141	58.8	337	59.1
2d reclosure	14	8.6	20	12.0	32	13.3	66	11.6
3d reclosure	8	4.9	11	6.6	17	7.1	36	6.3
4th reclosure	1	0.6					1	0.2
Lockout	40	24.6	40	23.9	50	20.8	130	22.8
Total	163	100.0	167	100.0	240	100.0	570	100.0

* From operating experience of the New York Power and Light Corporation, Albany, N. Y., covering portions of the lines to first sectionalizing point on the feeder. The values after "reclosure" indicate the number for which service was restored, or after "lockout" the number for which a permanent outage occurred.

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The author wishes to thank those who co-operated in making this study, especially Charles R. Craig who did many of the detailed calculations.

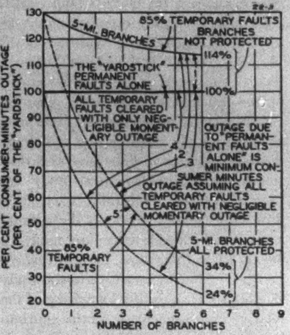

Figure 3. How to read the curves of Figures 4 to 13 inclusive

The curves shown duplicate those of Figure 4 for a nonreclosing substation breaker and single-element fusing at the branches

The "yardstick" equals the best service continuity obtainable with substation equipment alone (see Table II)

The upper pair of solid-line curves gives consumer minutes outage without branch protection and the lower pair of dash-line curves with branch protection. The important observations to be made in this and succeeding figures are (see reference figures on curve):

1. That the consumer minutes outage for the actual substation protection alone is higher than the "yardstick" because the breaker trips on some or all temporary faults as well as on permanent faults (see assumptions and Table IV in the appendix)
2. The improvement over actual substation protective equipment alone
3. The improvement over the "yardstick"
4. The improvement when permanent faults alone produce outages with branch protection—or line sectionalizing (this may be the improvement over the "yardstick" or over the actual substation protection)
5. The consumer minutes outage for manual restoration of service because temporary faults actually cause prolonged outages by tripping both the breaker or the branch or line sectionalizing device (note that for branch protection Figures 4 through 9, this is less than observation 1)

These observations can be converted into actual numerical values, as:

Per cent of ultimate improvement attainable =

$$\frac{\text{observation 2}}{\text{observation 4}}=\frac{114-34}{114-24}=86.7 \text{ per cent}$$

Table II. Calculated Values of Consumer Minutes Outage for Permanent Faults Alone With and Without Line Sectionalizing

(Based on Assumptions as Outlined in Appendix)

	Per Cent Temporary Faults					
	25	50	60	75	85	0–99.9+
Number of Sectionalizing Points	Consumer Minutes Outage for Permanent Faults Alone					*Per Cent Consumer Minutes Outage
‡ None (the "yardstick")	2,137.5	1,425	996.25	712.0	427.5	100
1	1,363	919	638.0	456.0	273.8	64.0
2	1,138	758	552.5	380.2	228.0	53.3
3	1,025	682	478.0	342.0	205.5	48.0
4	951	633	442.5	316.0	189.9	44.4
5	906	602	421.5	302.0	181.0	42.3
6	874	583	407.5	291.5	175.0	40.9

$$\text{* Per cent}=100\times\frac{\text{Actual consumer minutes outage calculated for setup}}{\text{The ``yardstick''}}$$

$$=100\times\frac{\text{Consumer minutes outage caused by permanent faults alone with line sectionalizing}}{\text{Consumer minutes outage caused by permanent faults alone without line sectionalizing (or branch protection)}}$$

For example:

$$=\text{for 1 sectionalizing point 25\% temporary faults}\frac{1{,}363}{2{,}137.5}=64.0\%$$

$$=\text{for 1 sectionalizing point 75\% temporary faults}\frac{456.0}{712.0}=64.0\%$$

‡ In a corresponding table for branch protection, the consumer minutes outage equivalent to these values—for "permanent faults alone" with no branch protection will vary with each different length and number of branches and will thus compensate for the increasing length of the total line (Figure 1). Consequently, the percentages can be compared, as in Figures 4 to 9, inclusive, to determine the relative improvement.

Table III. Maximum Number of Sectionalizing Points Possible With One-, Two-, and Three-Element Cutouts

	To Secure Proper Sequence of Operation	
Type of Cutout	Minimum Spread Between Fuse Ratings	*Maximum Number Sectionalizing Points Possible
One-element	Every second rating	7
Two-element	†Every second (sometimes every third)	Generally 7 (sometimes between 4 and 7)
Three-element reclosing	Every third or fourth rating	‡3or 4

* Based on 14(*N*) ratings of fuse links available from 10 to 100 amperes; 10-ampere assumed to be minimum sectionalizing fuse employed.

† See reference 7.

‡ Always at least one less than for two-element cutouts.

tions outlined in the appendix, the number of minutes outage per consumer per year (consumer minutes outage) on the whole circuit was determined for each type and for practically all combinations of types of protective devices now in general usage.

Generally, the problem of whether to use protective devices connected in series on the main feeder or on the branches centers around the advisability of combining these protective devices with nonreclosing equipment at the substation. Thus the "yardstick," chosen for comparison of the relative values of the different equipments and applications, relates everything to the best service continuity obtainable with substation protection alone, that is, without any line sectionalizing or branch protection.

In determining the best service continuity, it was taken into consideration that something might be done about automatic restoration of service after temporary faults, but permanent faults require the time and the work of a line crew before service can be restored. Thus when the consumer minutes outage caused by "permanent faults alone" is the "yardstick," it represents the minimum consumer minutes outage that can be attained with substation protection alone. Such a "yardstick" is universally applicable to any system and, therefore, was used as the 100 per cent base to which all types of protection were related in terms of a percentage of this base.

$$\text{Per cent}=100\times\frac{\text{Actual consumer minutes outage calculated for specific setup}}{\text{The ``yardstick''}}$$

$$=100\times\frac{\text{Consumer minutes outage caused by permanent plus temporary faults for any specific system setup}}{\text{Consumer minutes outage caused by permanent faults alone with no branch protection or line sectionalizing}}$$

Comparisons made in terms of this percentage tend to eliminate the effect of variations of actual practice from the assumptions employed in the study, in so far as this is possible. With any specific system setup, comparisons to the "yardstick," to each other, and to the ultimate attainable can be made of the following:

1. The improvement obtainable with reclosing versus nonreclosing relays and breakers.

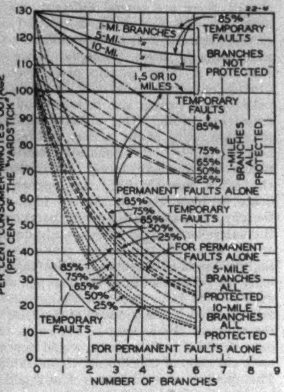

Figure 4 (left). Single-element fusing at branches with nonreclosing breaker at substation

The curves for no branch protection exceeds 100 per cent, because the nonreclosing breaker opens on temporary faults. Five observations should be made:

1. The very large reduction in consumer minutes outage for the "permanent faults alone" with branch protection and the very small additional outage time caused by the branch fuses blowing or breaker operation on temporary faults
2. The longer branches show much greater benefits in reduction of outage time
3. The spread between the curves for permanent faults alone and 85 per cent temporary faults is substantially less for the longer branches
4. The outage time due to temporary faults opening breakers or blowing branch fuses does not reach substantial values until 50 per cent, or until the temporary faults become greater in number than the permanent faults
5. Above 50 per cent the increased outage time on temporary faults increases much faster

2. The additional benefit with various amounts of branch protection or line sectionalizing.

3. The benefit of different combinations of substation and line equipment.

4. The effect of maintenance of line to reduce the percentage of temporary faults.

Curves Provide for Studying Individual Problems

It will be impossible to discuss adequately in the span of one paper all phases of the problem covered by this study. Even if this were attempted, there would be many other questions that would present themselves in actual operating practice on different systems. Therefore, curves (Figures 4 to 9 inclusive for branch protection, and Figures 10 to 14 inclusive for line sectionalizing with a number of protective devices connected in series) have been plotted, in view of their future usefulness in studying specific problems on different systems that may not be covered in the general conclusions to be drawn. The curves present a large share of the data compiled in this study, which may not fit a specific system exactly but should provide a fairly close approximation. The curves for one particular system setup, such as branch protection with nonreclosing breakers, Figures 4 and 8, are all plotted on one or two curve sheets to permit visual determination of the relative benefits with different equipments, percentages of temporary faults, lengths of branches, and so forth. Numerical values can be determined or visualized as described in Figure 3 on "How to Read the Curves."

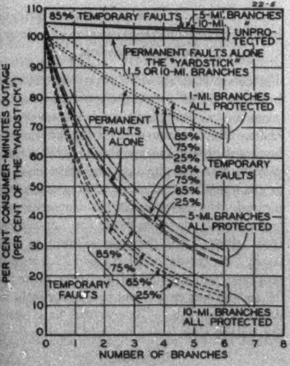

Figure 5 (lower left). Single-element fusing at branches with reclosing breaker at substation

The reclosing breaker obviously reduces to a negligible value the outage-time when clearing temporary faults. Observe three things:

1. That the outage time caused by the blowing of branch fuses on temporary faults is of not much importance up to 75 per cent or even 85 per cent values
2. That the spread for temporary faults covers a very narrow range and does not vary materially with the length of the branch as it did in Figure 4
3. That the improvement provided by the reclosing breaker is not so great with an increasing number of branches, because the branch protective devices afford this improvement with the nonreclosing breaker

For example, compare the top line (for 85 per cent temporary faults) of the groups marked "five-mile branches all protected" in Figures 4 and 5. With 0 branches the curves start at 130 per cent in Figure 4 and at 104 per cent in Figure 5 or a difference (improvement) of 26 units. With three branches the improvement is ten units (60 per cent in Figure 4 and 50 per cent in Figure 5) and with six branches it is six units (34 per cent in Figure 4 and 28 per cent in Figure 5)

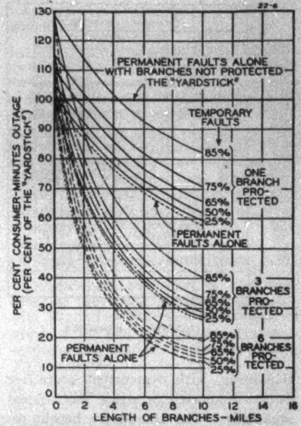

Figure 6. Value of branch protection with different lengths and number of branches with single-element fuse cutouts. Nonreclosing breaker at substation

These curves are similar to those in Figure 4, except that the variable is the length rather than the number of branches. A reclosing breaker at the substation would show, of course, the same improvement over the values in Figure 5 as the above shows over Figure 4. Observe that, while the major improvement is provided with branches four to ten miles in length, there is an appreciable gain over substation protection alone, even with branches one mile long especially where there are several of them

The Effect of Feeders Shorter or Longer Than 30 Miles

The curves in Figure 14 are intended for modification of the other curves (Figures 10 to 13 inclusive) when sectionalizing feeders shorter than 30 miles. If the percentages in Figure 14 are added directly to the values of Figures 10, 11, 12, and 13, the result will be reasonably accurate. For rural feeders longer than 30 miles the reduction from the percentages of Figures 10, 11, 12, and 13 would be so small that they can be neglected.

Combining Branch Protection and Line Sectionalizing

Individual protection of several comparatively short branches and of line sec-

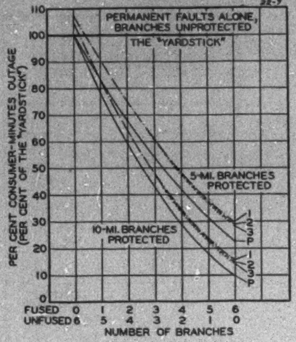

Figure 7 (left). Partial branch fusing, using similar cutout at substation—75 per cent temporary faults

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse
P = Permanent faults alone

Many rural lines employ the same type of protection at the substation as that used on the branches, as is illustrated by these curves. It is obvious from these curves that all the branches should be protected. For the effect with less than six branches see Figure 8. Observe that two- or three-element fuses do not show much improvement over single-element fuses

Curves for the resetting recloser would fall just below those for the three-element fuses as in Figure 8

tionalizing with overcurrent protective devices connected in series, was studied separately. The cumulative benefit resulting from a combination of these can be approximated. The curves for such a combination (drawn similar to Figures 4 to 9 inclusive) would start at a percentage value for zero branches which is identical to that shown for the degree and type of sectionalizing employed in Figures 10, 11, 12, and 13. The whole curve would not be moved down bodily as the improvement would decrease with an increasing number of branches.

With a circuit comprising several long branches, the effect of the individual branch protection will be governed by the data in curves of Figures 4 to 9 inclusive. The use of devices connected in series on individual branches should be studied by treating each branch separately in accordance with the data in Figures 10 to 14 inclusive.

Some Protective Devices Can Be Connected in Series in Greater Numbers Than Others

An important factor that should not be overlooked in comparing different equipments, when employed for line sectionalizing, is the number that can be connected in series and still provide proper selectivity of operation[5–6] from the service entrance fuse to the feeder relay and breaker. For example:

1. In comparing two- and three-element reclosing fuse cutouts, it is possible to secure discriminative operation with more of the two- than of the three-element devices connected in series. If an equal number could be employed, the extra reclosure of the third element would account for 10 to 18 per cent reduction in consumer minutes outage (at 85 per cent temporary faults) tapering down to zero (at 25 per cent temporary faults) as shown in Figures 10 and 11. However, splitting the line up into a greater number of sections with the two-element cutouts[6–7] (see Table III) generally affords as good or better service continuity than with three-element cutouts. Sometimes an attempt is made to secure closer fusing by allowing an individual fault to blow two or more section fuses simultaneously, depending on the reclosing fuse nearer the source of supply to maintain service. This voids the benefit of the reclosing cutout closer to the substation, unless all such blown fuses are always renewed before an outage occurs.

2. In regard to automatic resetting reclosers, there are several different ratings available, such as 3, 6, 12, 25, and 50 amperes. Generally, all of these will co-ordinate properly when connected in series. However, it is not always practicable to provide automatic selectivity of operation between the smaller size reclosers and the transformer fuse. This either limits the minimum rating of the recloser usable, and thus the number of sectionalizing points, or necessitates manual closing of the circuit through a fuse shunting the recloser, in order to blow the transformer fuse and thereby locate the fault. In this latter case, the values determined in this study do not apply, since the transformer faults would slightly increase the number of line outages with the reclosers, above the one per mile used for the other types of equipment or system setup. The amount of decrease in the value of the recloser as shown in the data obtained in this study, would depend upon the ratio of transformer-to-line faults. The number of transformer faults which would cause lockout of the recloser are, in general, substantially less than the number of line faults.

For Reclosers Which Do Not Reset Automatically

The conclusions drawn and the data in the curves for reclosing fuse cutouts apply

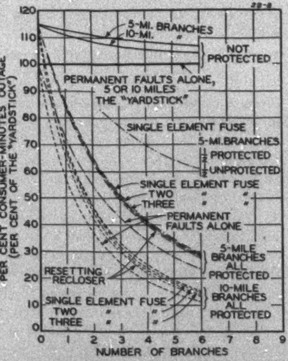

Figure 8. Different types of branch protection with nonreclosing breaker at substation for 75 per cent temporary faults

If a reclosing breaker were used at the substation, the curves would be shifted as in Figure 5. Observe that the reduction in outage time by the reclosing devices below that provided by single-element fuse is very small, due to the few consumers affected by the outages on one branch, caused by temporary faults that otherwise would have had service restored automatically by the reclosing devices (this difference would increase slightly with 85 per cent temporary faults and become negligible at lower percentages)

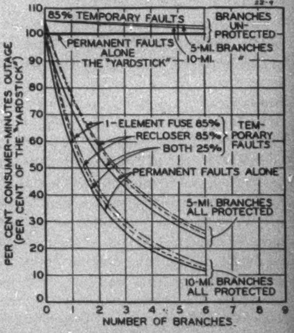

Figure 9. Branch protective devices delayed beyond first instantaneous opening of reclosing substation breaker

The curves start above 100 per cent at zero branches and 85 per cent temporary faults, because the breaker locks out on some temporary faults. The curves for two- and three-element fuses would fall between those for the single-element fuse and the resetting recloser, but, as the range is too narrow, they were not plotted. The curves for 25 per cent temporary faults are so close to those for "permanent faults alone," that they are plotted as one curve. The curves for 50, 65, and 75 per cent temporary faults would fall between those for 25 and 85 per cent about proportionately to those in Figure 5. Observe that here again the outage time is not reduced much below Figure 5 to any type of apparatus with an equal number of reclosers and without the feature of automatically resetting after clearing a temporary fault.

Conclusions

The study permits drawing general conclusions, which may be helpful in system planning.

Is Substation Protective Equipment Enough?

1. Combining overcurrent branch protection or line sectionalizing—with reclosing breakers at the substation—provides a cumulative reduction in consumer minutes outage that neither method alone can provide (compare curves of Figures 4 with 5, 8 with 9).

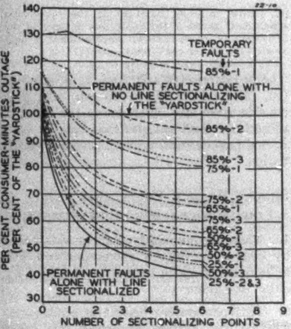

Figure 10. Line sectionalizing with single-element fuse cutouts and with two- and three-element reclosing fuse cutouts with same type at substation as at sectionalizing points

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse

These curves represent conditions found on many rural circuits where the same type of protection is employed at the substation and out on the lines. A nonreclosing breaker could be substituted for the single-element fuse at the substation without changing these curves but, when substituted for the two- and three-element fuses, would raise these curves. The curves exceed the "yardstick" (100 per cent) because of the temporary faults causing outages. Observe three things:

1. The very large reduction in consumer minutes outage for "permanent faults alone" with line sectionalizing, but the much greater increase in this outage time caused by opening on temporary faults by sectionalizing fuses as compared with branch fuses, Figure 4

2. Single-element fusing provides an improvement (for all but one sectionalizing point at 85 per cent temporary faults) which with 65 per cent and lesser percentages of temporary faults is more than half of that obtainable

3. Two- and three-element fuses show much more benefit for sectionalizing than for branch protection

2. The cumulative reduction from this combination is not so great as with an increasing number of branches or sectionalizing points, but a number of branches do provide substantial combined benefit (see discussion in captions of Figures 5 and 11).

3. Overlapping the substation reclosing protection with line protective devices provides some additional improvement with branch protection and a major improvement with line sectionalizing. With such overlapping, the breaker trips and recloses once, without the branch or line protective device opening for all faults out to the ends of the line, and then the relay provides time delay, so that the branch or line sectionalizing protective device disconnects the faulted portion of the circuit ahead of the second tripping of the relay. Single-element fusing at the branches or sectionalizing points with this overlapping protection approaches very closely to providing the minimum consumer minutes outage attainable for the specific system setup.

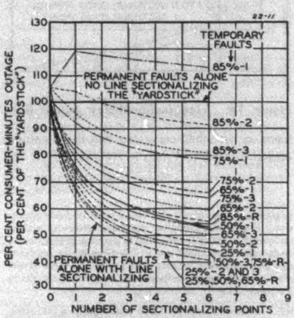

Figure 11. Line sectionalizing with reclosing breaker at substation

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse
R = Resetting recloser

The curves exceed the "yardstick" (100 per cent) with 0 sectionalizing points due to the breaker locking out on some temporary faults (see Table IV in appendix). The reclosing breaker obviously reduces to a negligible value the outage-time when clearing temporary faults with no sectionalizing. Observe:

1. That this reduction is not so great with an increasing number of section points (as with branch protection, Figure 5), because line sectionalizing affords some of this reduction anyway, and because the benefit is restricted to the first section beyond the substation, which becomes shorter as the number of section points is increased

2. That single-element fusing provides almost an equal degree of improvement over the reclosing breaker alone as with a nonreclosing breaker, Figure 10

3. That resetting reclosers show a substantial improvement over three-element fuses with temporary faults exceeding 50 per cent

4. Where such overlapping protection as in paragraph 3 reaches only part way out on the line, the improvement over a reclosing breaker will be approximately proportional to the percentage of the line so protected, that is, if half the line is so protected, the improvement will be about one half of the difference in percentages between that percentage with a reclosing relay Figure 5 or 11, and that with the overlapping protection covering the whole line, Figure 9 or 12 respectively.

Of What Value Is Branch Protection?

1. Overcurrent protection of individual branches provides a greater improvement over substation protection alone, than is provided by line sectionalizing (compare corresponding curves Figures 4–9 with Figures 10–13).

2. The length of the branch has a major effect on the benefit secured (see Figures 4, 5, and 6).

3. Protecting even a number of short branches provides a cumulative improve-

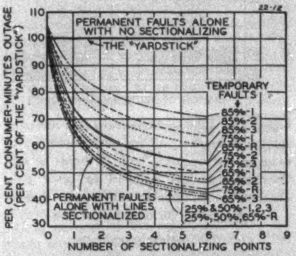

Figure 12. Line sectionalizing with reclosing substation breaker, set for instantaneous tripping on first opening, with all types of sectionalizing devices delayed to open only ahead of second opening

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse
R = Resetting recloser

It is assumed that all faults cause operation of the instantaneous trip, corresponding to ground relaying only. The improvement shown will be decreased approaching the corresponding curves of Figure 11, about proportionately to the percentage of faults that are line to line. Observe three things:

1. The major reduction in outage time with all types of sectionalizing equipments, because this overlapping protection is the equivalent to adding one automatic resetting reclosure to the device at each section point

2. The outage time is negligible due to fuse blowing or breaker opening on temporary faults even with single-element fuses at 65, and lesser percentages of temporary faults

3. Reclosing fuses or resetting reclosers at the section points become effective at 75 and higher percentages of temporary faults

ment that is likely to justify at least single-element fusing (see Figures 4, 5, and 6).

4. Individual protection should be applied to all the branches. Protecting only a portion of the branches affords less benefit, Figure 7, than protecting all the branches, Figure 4 (see also Figure 8).

5. Single-element fusing of branches provides approximately 85 to 95 per cent of the total improvement obtainable (see value worked out in example in Figure 3; see also Figures 4, 5, and 8).

6. Two-element reclosing fuse cutouts provide a slight additional improvement in service continuity (see Figures 7 and 8).

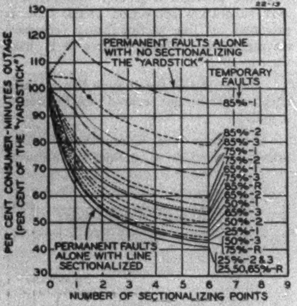

Figure 13. Line sectionalizing with reclosing substation breaker, set for instantaneous tripping on first opening, with all types of sectionalizing devices delayed to open only ahead of second opening of breaker. Breaker protection for only half of line

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse
R = Resetting recloser

Quite often the minimum obtainable pickup current setting of the relay only permits protecting part of the line or on delta circuits the overlapping protection only reaches out part way on the line. These curves show the effect of such relaying covering the first half of the 30-mile feeder. This does not overlap the sectionalizing device with just one point which is located at the center of the line so that the values under this condition are the same as in Figure 11. It should be noted that these curves are located about half way between the respective curves of Figures 11 and 12, or about proportionately to the section of the line on which the overlapping protection is provided

7. Three-element reclosing fuse cutouts afford so little benefit over the two-element reclosing fuse cutout that it raises the question as to the justification for the extra premium paid for the third fuseholder, Figures 7 and 8. (This is true even though the assumptions of Table IV in the appendix favor the three-element device.)

8. Reclosures which reset automatically after clearing a temporary fault provide only a slight additional improvement over that accomplished by reclosing cutouts, even when fuses are not renewed until an actual outage occurs (see Figure 9).

OF WHAT VALUE IS LINE SECTIONALIZING?

1. Sectionalizing the main feeder and long branches with protective devices connected in series generally affords additional improvement over the best service continuity that can be afforded by overcurrent protective equipment located at the substation.

2. Sectionalizing with single-element fuse cutouts is generally very effective. They

(*a*). Will afford the major portion of the total obtainable improvement at 65 or a lesser percentage of temporary faults with nonreclosing or reclosing substation breakers (see Figures 10 and 11).

(*b*). Will be more effective at 80 per cent temporary faults with the reclosing substation breaker which provides overlapping operation, that is, tripping ahead of all the fuses on the first operation, than three-element reclosing cutouts with a reclosing breaker but no overlapping protection—under similar conditions, at 65 or a lesser percentage of temporary faults will equal the best of reclosing equipment without overlapping protection (compare Figure 12 with 11).

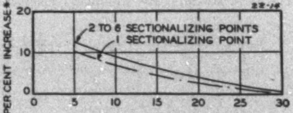

Figure 14. Line sectionalizing shorter lines

Same as on other curves so that these can be added directly to values in Figures 10, 11, 12, and 13

(*c*). Require more than two points with a reclosing breaker or more than one point with a nonreclosing breaker in order to improve rather than impair service at 85 per cent temporary faults (see Figure 10).

3. Sectionalizing with two and three-element reclosing fuse cutouts is much more effective than the use of similar equipment for branch protection. Each affords respectively

(*a*). Some additional improvement over no sectionalizing at 85 per cent temporary faults with a reclosing cutout or breaker at the substation (see Figures 10 and 11).

(*b*). About 25 to 35 per cent additional improvement over the effective operation of the single-element fuse cutout at 75 per cent temporary faults with a reclosing cutout or breaker at the substation (see Figures 10 and 11).

(*c*). About 10 to 15 per cent additional improvement over the much greater effectiveness of the single-element cutout at 85 per cent temporary faults with overlapping reclosing breaker operation (see Figure 12).

The additional improvement referred to in (*a*), (*b*), and (*c*) above is in per cent of the ultimate obtainable between the curves for "permanent faults alone," with and without line sectionalizing. These percentages do not include any of the advantages possible from discovery and renewal of blown fuses with a service interruption (see Table V in the appendix).

4. Generally, two-element reclosing fuse cutouts are more effective than three-element designs because a greater number of the two-element cutouts can be connected in series and still provide selectivity of operation (see Table III). Figures 10 and 11 show this even with the more favorable assumptions for the three-element fuse of Table IV in the appendix.

5. Sectionalizing with reclosers, which reset automatically after clearing a fault, is most effective at the higher percentages of temporary faults where the resetting feature has a greater opportunity to decrease the number of outages of long duration caused by temporary faults, Figure 11.

(*a*). With 85 per cent temporary faults and a reclosing substation breaker, this amounts to 40 to 60 per cent additional improvement over three- and two-element cutouts respectively (see Figure 11).

(*b*). This additional improvement tapers down from about 25 and 35 per cent at 75 per cent temporary faults, to zero at 25 per cent (see Figure 10).

(*c*). With reclosing breakers at the substation which clear ahead of the protective devices on the first opening, the additional improvement is only about 15 to 25 per cent (see Figure 12).

The percentages of additional improvement (*a*), (*b*), and (*c*) above do not take into consideration any difference in the number of reclosers and cutouts that can be connected in series and provide proper co-ordination.

SHOULD BRANCH PROTECTION AND LINE SECTIONALIZING BE COMBINED?

1. There is a cumulative benefit obtainable by combining branch protection and line sectionalizing which cannot be provided by either one alone.

2. This cumulative benefit is obtainable, whether the system comprises one main feeder with a number of short branches, or two or more long branches with a short main feeder. (With the latter, the branches would be sectionalized with protective devices connected in series.)

3. The cumulative benefit does not increase directly with an increasing number of protected branches.

SHOULD THE PERCENTAGE OF TEMPORARY FAULTS BE CONSIDERED?

1. Any decrease obtainable in the percentage of temporary faults tends to permit securing an equal improvement in service continuity with lower cost overcurrent protective devices.

2. It might prove valuable to make a study in which the costs of improving line construction, tree trimming, and so on are compared with the savings in protective equipment which the improvements made possible.

Use of Curves for Specific Studies

It has been shown by this study that individual protection of branches and/or line sectionalizing can be combined effectively with the reclosing substation relay and breaker to reduce the consumer minutes outage on distribution systems. The data presented in the curves should facilitate the studying of specific problems.

This may make possible more efficient use of available equipment to meet the particular needs and justifiable economics of different circuits and systems.

Appendix. Assumptions Employed in Mathematical Study

1. *The Distribution Lines Studied (Length of Lines).*

(*a*). In one part of the study, one to six unprotected and protected branches, each one, five, or ten miles in length, were added at the mid-point of an unsectionalized 30-mile main feeder, increasing the total length of line, as in Figure 1. (The difference in total length might introduce a slight error in comparing the benefit provided with and without branch protection, but not in comparing the relative benefits afforded by the different equipments that might be employed.) Connecting the branches at the mid-point provided an average for an even spacing of the branches along the feeder.

(*b*). In the other part of the study, a 30-mile main feeder was broken up into from one to seven sections by zero to six overcurrent protective sectionalizing devices connected in series as in Figure 2. (The effect of the shorter, 5 to 30-mile feeders, was checked.)

2. *Equipment Employed.* Both studies included checking comparative results with overcurrent protection provided by:

(*a*). Substation breakers actuated by nonreclosing relays, automatic reclosing relays, and automatic reclosing relays which overlap all branch and line protective equipment, so that the breaker trips and recloses once without the branch or line devices opening, and then provides time delay before the second tripping of the relay on more persistent faults; each combined with

(*b*). Sectionalizing or branch-line single-element or two- and three-element reclosing fuse cutouts or reclosers which reset automatically after clearing a temporary fault.

3. *Number of Consumers.* Three per mile uniformly distributed. (Any other number per mile might have been used without affecting the percentages used for comparison.)

4. *Number and Type of Faults.* One per mile per year uniformly distributed with temporary faults equaling 25, 50, 65, 75, and 85 per cent of the total. (One per mile is probably somewhat high but any other value might have been used without changing the percentages used for comparison.)

5. *Attended Substation.* A period of five minutes was allowed after the fault occurred for notification and for the attendant to close the breaker, thus restoring service if a temporary fault had cleared itself after the breaker locked open. (Notification by an alarm system at the substation would lower the percentages used for comparison to the extent that the manual closing of the breaker approached that of a momentary outage. Conversely, any lengthening of the time, such as that required to get to an unattended station, would raise the percentages. In both cases, the degree of change would decrease slightly with an increasing number of protected branch or sectionalizing points.)

6. *Trouble Crew.* Always available at the substation to start instantly with no time allowed for notification or preparation. (More often the crew will be out on the system, sometimes closer to and sometimes farther from the fault location, so this assumption provided an average. If any longer time than the zero had been employed it would have indicated slightly greater benefits for branch protection, line sectionalizing, and single-element fusing.)

7. *Locating Fault and Restoring Service.* The trouble crew:

(*a*). Traveled 30 miles per hour to the sectionalizing point at which the protective device had opened. No time was allowed for examining sectionalizing or branch protective devices en route, as it was assumed these would have indicating features visible from the car.

(*b*). Spent five minutes to climb the pole and to restore service if a temporary fault had caused the outage at the sectionalizing or branch protection point.

(*c*). Traveled 15 miles per hour to the mid-point of the branch or section on which the fault persisted. The mid-point provided the average for the uniform spacing of faults.

(*d*). Spent 30 minutes repairing a permanent fault with the outage persisting on the entire faulted portion until the fault was repaired. (The inclusion of some sort of manual sectionalizing or cutting out of the faulted portion would have decreased the number of consumers affected, and thus decreased very slightly the improvement indicated, as resulting from line sectionalizing and branch protection. For example, if the permanent faults had been repaired in zero time for all consumers, the percentages used for comparison would have been raised only about five units.)

(*e*). After repairing the fault:

1. Either the substation attendant was notified instantly when he was required to close the breaker (although some time probably would be required for notification, which would have enhanced the value of line sectionalizing and branch protection).

2. Or the crew traveled 30 miles per hour to return to the sectionalizing or branch protective device to restore service.

Table IV. Assumptions and Operating Experience on Service Restoration

By Reclosing the Circuit	Service Restored Per Cent of Total Faults on Line‡	
	Values Used in Calculations (Per Cent)	Values From Operating Experience* (Per Cent)
Once	50	50 to 60
Twice	15 Additional	10 to 15 Additional
3 Times	5 Additional	5 Additional
4 Times	3 Additional	1 Additional

‡ If the percentage of temporary faults is less than these values, service is restored only up to the actual number of temporary faults. If greater than the sum of the percentages of restoration for that number of reclosures, it was assumed that the remaining temporary faults persisted longer than the lockout time of the protective device, causing a permanent outage until the substation breaker, line sectionalizing device, or branch protective device was closed manually. Thus perfect operation ("permanent faults alone" causing an outage) was assumed to be provided up to the sum of the 50+15+5 values or 70 per cent of the total number of faults for devices which reclose three times (50 per cent for those which reclose once, 65 per cent for those which reclose twice, and 73 per cent for combinations which reclose four times).

* See Table I in text and reference 3.

Maximum advantage was given to three-element reclosing fuses and other multireclosing devices as compared with two-element reclosing fuse cutouts, since the "values used in the calculations," Table IV, are low for the first reclosing, and high for the second as compared with the "values from operating experience." Some data show as high as 75 to 90 per cent restoration of service following the first reclosure. This would make all the curves for two-element reclosing fuses at branch or line sectionalizing points, as in Figures 7, 8, and 10 to 13 inclusive, approach more closely the curve for "permanent faults alone" for that setup. Of course, there would be a corresponding improvement with three-element fuses and resetting reclosers in approaching or equaling the curves for "permanent faults only."

8. *Service Restoration by 1st, 2d, 3d and 4th Reclosures* was assumed to be in accordance with the values in Table IV. Service restoration is expressed as a percentage of the total faults on the line.

9. *No Inspection With Reclosing Fuse Cutouts Was Assumed.* Because such inspection would have permitted re-fusing before an outage occurred, the data indicate only the minimum benefit for these devices. (This benefit would be increased to a degree approaching that of the device that resets automatically as such fuse renewal before the occurrence of an outage approaches 100 per cent. The percentage of such discovery and renewal is fairly high, because linemen and trouble crews are on the lookout for the indicating devices as they pursue their regular duties along the lines, Table V. Thus all reclosing fuse data are ultraconservative.)

10. *The Protective Orbit of the Substation Breaker* (which is determined by minimum pickup current of the relay) included the whole feeder and all of the branches. In many instances, distribution lines have outgrown this orbit. Consequently, the lower current individual protection of the branches and line sectionalizing on the otherwise unprotected portion may prevent burning down lines, annealing of the conductors, and loss of revenue, in addition to any advantages shown by the study. Conversely, this lower current protection will cause some outages (momentary or prolonged, depending on the type of equipment) for faults which would have burned clear. The combined effect of these few additional operations will balance off to the extent that faults which otherwise would have burned clear,

Table V. Operating Record of Two-Element Reclosing Cutouts on Line With Practically No Special Patrolling

Year	Total No. Faults	No. of Permanent Faults	Number of Temporary Faults Restored By Cutout				
			Discovered Without Outage	Not Discovered Before 2d Fuse Blew	Total Restored	Caused Outage	Total
1939	36	8	8	10	18	10	28
	100%	22.3%	22.3%	27.7%	50%	27.7%	77.7%
1940	59	15	14	15	29	15	44
	100%	25.5%	23.7%	25.4%	49.1%	25.4%	74.5%

Service was restored on $^{2}/_{3}$ of the temporary faults, approximately $^{1}/_{3}$ being made possible by discovering and renewing first fuse link before an outage occurred, and $^{1}/_{3}$ by the reclosing operation of the cutout.

Current-Transformer Performance Based on Admittance-Vector Locus

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Synopsis: In the past designers as well as users of current transformers have employed ratio error and phase-angle curves in which the abscissa represents primary or secondary current to a linear scale. Numerous curves were necessary both for obtaining a clear picture of the performance characteristics as well as for determining the errors for the multiplicity of possible secondary burdens. Part I of this paper shows that a more functional picture of current-transformer operation is obtained by replacing these commonly used curves by the admittance-vector locus of the secondary winding with the primary open circuited, the end point of the vector representing the independent variable in a curvilinear co-ordinate system. The numerous ratio and phase-angle curves resulting from various secondary burdens and, in case of multiratio transformers, from different numbers of turns, when referred to this new co-ordinate system revert to one single curve. Ratio error and phase angle for any burden at any power factor, turn ratio, and secondary current can be scaled or read directly from a chart using as a basis the admittance vector locus of the steel forming the magnetic circuit of the transformer. For designers as well as users it is often advantageous to be in possession of analytical expressions for ratio error and phase angle. In part II of the paper general formulas are set up which express the performance in terms of the various constants and variables of a transformer, making it unnecessary to refer to charts for the analysis of important design or performance factors.

Part I

BASED on the fundamental paper by P. G. Agnew[1] published in 1911, a practically standard procedure of computing current-transformer performance has been established in the past three decades[2] consisting of the following steps:

1. The transformer is most readily analyzed by introducing the 1/1 ratio type in which the number of primary turns is made equal to the number of secondary turns.

2. The above transformer can be represented by the equivalent circuit shown in Figure 1 and the corresponding vector diagram Figure 2 in which

$OA = I_2 =$ secondary current
$OB = I_e =$ exciting current
$OC = I_m =$ magnetizing-current component of I_e
$CB = I_w =$ watt-current component of I_e
$OD = I_2R_B =$ voltage drop across resistance of burden
$DE = I_2X_B =$ voltage drop across reactance of burden
$EF = I_2R_2 =$ voltage drop across resistance of secondary winding
$FG = I_2X_2 =$ voltage drop across leakage reactance of secondary winding
$OE = I_2Z_B = E_B =$ voltage drop across burden
$OG = I_2Z = E_2 =$ voltage drop across total secondary impedance
$\phi =$ phase-angle lag between E_2 and I_2
$\theta =$ deviation of phase angle between I_1 and I_2 from 180 degrees
$\phi_0 =$ phase-angle lag of exciting current referred to the reversed secondary voltage E_2

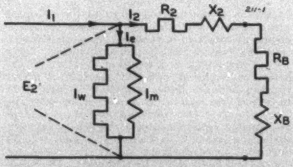

Figure 1. Equivalent circuit of a 1/1 ratio current transformer

3. Magnetizing and watt components of the exciting current are computed from charts showing watts, reactive volt-amperes, and volt-amperes per pound for the steel used in function of the flux density; see Figure 3. The conventional procedure consists of computing the voltage E_2 for the secondary current and burden for which the errors are to be determined, and calculating the flux density from

$$B = \frac{E_2 10^8}{A N f\, 4.44} \text{ gausses} \tag{1}$$

where

$A =$ cross section of core in square centimeters
$N =$ number of secondary turns
$f =$ frequency

and obtain I_m and I_w as follows:

$$I_w = W/E_2;\quad I_m = RVA/E_2;\quad I_e = VA/E_2 \tag{2}$$

The curves shown in Figure 3 apply to a conventional grade of silicon steel and will be used as a basis in the following. If a sample of the transformer to be investigated is on hand I_w, I_m, and I_e are available from the excitation curves of the secondary winding.[3]

4. Ratio and phase-angle can be computed by means of the following formulas:

$$R(1/1) = \frac{I_2 + I_m \sin\phi + I_w \cos\phi}{I_2 \cos\theta} \tag{3}$$

$$\tan\theta = \frac{I_m \cos\phi - I_w \sin\phi}{I_2 + I_m \sin\phi + I_w \cos\phi} \tag{4}$$

$$R_{(N)} = \frac{N}{N_1} R(1/1) \tag{5}$$

where $N_1 =$ number of primary turns.

$$\text{Ratio error} = \Delta(1/1) = R(1/1) - 1 \tag{5a}$$

In order to obtain curves showing the errors over a current range, the above procedure must be repeated for numerous secondary-current values. In the following it will be shown that a clearer picture of current-transformer performance results if numerical calculations of ratio error and phase angle are not executed at such an early stage. The method used is similar to that of representing the per-

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cause greater or lesser consumer minutes outage than faults for which additional protection is provided. No data are available on this relationship. However, they probably would have slight effect on the percentages used for comparison.

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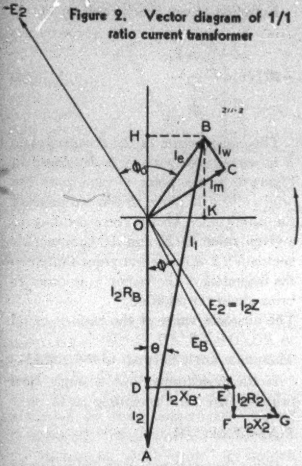


Figure 2. Vector diagram of 1/1 ratio current transformer

formance of power transformers and induction motors by means of circuit loci, or specifically by means of the circle diagram. Referring to Figure 2, extending calculations into the complex plane, the complex ratio $\boldsymbol{R}(1/1)$ can be expressed as follows:

$$\boldsymbol{R}(1/1)=\frac{\boldsymbol{I}_1}{\boldsymbol{I}_2}=\frac{-\boldsymbol{I}_2+\boldsymbol{I}_e}{\boldsymbol{I}_2}=-1+\frac{\boldsymbol{I}_e}{\boldsymbol{I}_2} \tag{6}$$

Since $\boldsymbol{I}_2\boldsymbol{Z}=\boldsymbol{E}_2$, (6) can be written as follows:

$$\boldsymbol{R}(1/1)=-1+\frac{\boldsymbol{I}_e}{\boldsymbol{E}_2}\boldsymbol{Z} \tag{7}$$

The vector $\boldsymbol{I}_e/\boldsymbol{E}_2$ can be recognized as the negative admittance vector of the secondary winding with the primary winding open circuited; denoting it with $\boldsymbol{Y}$ it follows:

$$\boldsymbol{R}(1/1)=-1-\boldsymbol{YZ} \tag{8}$$

The ratio of a 1/1 transformer is equal to the negative real number one decreased by the product of the admittance vector and the total secondary impedance vector.

It is customary to represent the performance of transformers at various fixed secondary burdens Z_B. In bushing-type transformers with equally distributed secondary winding, the leakage reactance is negligible in comparison to the burden impedance and in wound-type transformers it can, in many cases, be considered constant over the normal metering range. Under these conditions Z remains constant when considering ratio error and phase-angle changes due to variations in the secondary current. The admittance vector becomes the basic current-transformer curve, curves for various burdens being similar. An investigation of the admittance-vector locus is therefore indicated.

The Admittance-Vector Locus

It appears advisable to refer to a specific transformer at this time. Due to its most frequent use a multiratio bushing transformer will be investigated, but none of the generalities of the study is thereby sacrificed. The transformer has N secondary turns equally distributed on a core of $17^1/_4$ inches inside diameter, $21^3/_4$ inches outside diameter, and $7^3/_4$ inches height, giving a cross section of 112.5 square centimeters and a weight of 265 pounds; it is used in conjunction with 115-kv oil circuit breakers. The admittance-vector locus is determined in accordance with the following procedure:

From formula 1 the flux densities B are determined for various assumed secondary voltages E_2; referring to chart, Figure 3, the corresponding VA/lb and W/lb values are determined. The power factor is computed for each voltage, and the total volt amperes are obtained by multiplying the VA/lb by the weight. The magnitude of the admittance vector is equal to

$$Y=VA/E_2 \tag{9a}$$

If curves showing the exciting current and its components in relation to the secondary voltage are available,[3] the admittance vector and its components can be obtained as follows:

$$\boldsymbol{Y}=\boldsymbol{I}_e/\boldsymbol{E}_2;\ \text{conductance } G=I_w/E_2;\ \text{susceptance } B=I_m/E_2 \tag{9b}$$

For each voltage E_2 the calculated admittance vector is plotted. Due to its frequent use the admittance vector corresponding to one secondary turn has been computed and is shown in Figure 4. As can be seen, the locus has the shape of a hairpin, the admittance decreasing from 365 mhos for approximately 0.003 volt to 30 mhos for 1.5 volts and increasing again to large values for increasing voltage.

Similarly, the phase angle with respect to the secondary-voltage vector (positive real axis) decreases to a minimum with increasing voltage and increases with further increase in voltage. It is apparent that this curve is a function of the characteristics of the steel only; a more basic curve is shown in Figure 5, where the flux density is chosen as the independent variable. In the range shown the admittance decreases for flux-density values of from 10 to 5,000 gausses, where a minimum is reached, and increases thereafter with increasing flux density. Minimum admittance corresponds approximately to maximum permeability of the steel; the right-hand branch of the curve indicates the region below saturation, the left-hand branch the region in which saturation occurs. ϕ_0 is the phase angle of the exciting current of the steel. It is advantageous to refer to this basic curve, having B or a quantity directly proportional to B as independent variable. Obviously, the value of volts per turn E_2/N is such a quantity, having the advantage that it is related most directly to the important variables of the problem, namely applied voltage and secondary turn numbers. If the admittance vectors for a given core and varying turn numbers are compared at identical flux densities or volts-per-turn values,

Figure 3 (below). Volt-amperes, reactive volt-amperes, and watts for one pound of steel in relation to flux density

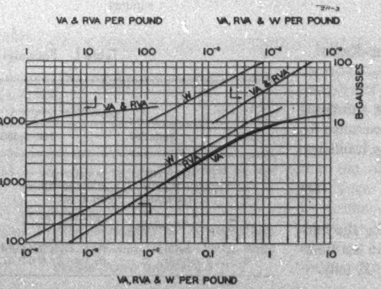
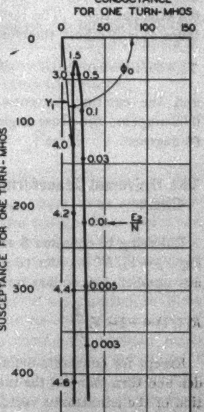
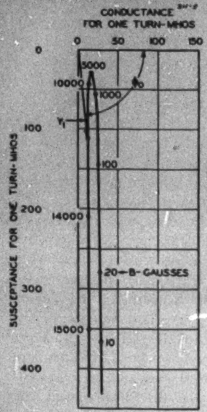


Figure 4 (right). Admittance vector Y_1 in mhos for one turn as function of volts per turn for 115-kv bushing transformer

Admittance for N turns computed according to $Y_N=Y_1/N^2$





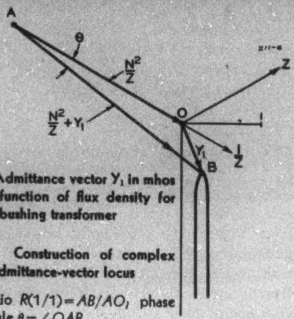


Figure 5 (left). Admittance vector Y_1 in mhos for one turn as function of flux density for 115-kv bushing transformer

Figure 6 (right). Construction of complex ratio from admittance-vector locus

Magnitude of ratio $R(1/1)=AB/AO$, phase angle $\theta=\angle OAB$

then the following well-known formula results:

$$Y_N(B)=Y_1(B)/N^2 \tag{10}$$

This relation is indicated in Figure 4 showing Y_1, making it useful for determining the admittance for any number of turns. In order to profit by the application of this equation, volts-per-turn values are used as independent variable in the following.

Aside from its application in the following analysis, Figure 4 is valuable for determining the exciting-current vector in idle transformers in conjunction with multiple interconnected circuits, such as in bus-differential scheme protection.[4]

The use of this chart shall be shown in conjunction with an example:

Determine the exciting current of the above transformer for a voltage of 20 volts and 40 turns. E_2/N is computed to be 0.5 volt per turn. The resulting admittance Y_1 is 49 mhos; therefore

$$Y_{40}=49/40^2=0.031 \text{ mho}$$

and the exciting current $=0.031\times 20=$ 0.62 ampere; the phase angle amounts to 66 degrees.

The Universal Transformer-Ratio Chart

Referring to equation 8 and substituting $Y_N=Y_1/N^2$ in order to generalize for any number of secondary turns, it follows:

$$R(1/1)=-1-Y_1\frac{Z}{N^2} \tag{11}$$

Except for constants defining the burden and turn number, the ratio is a function of the admittance vector Y_1 only.

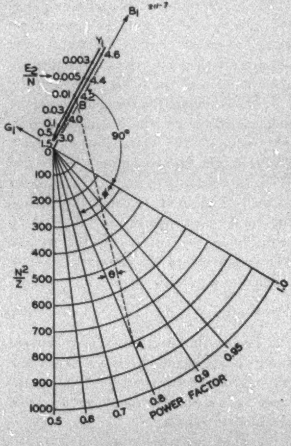


Figure 7. Chart for calculation of ratio and phase angle for any number of secondary turns N, any total burden Z at any power factor and any desired secondary current I_2, provided $N^2/Z<1{,}000$

Example: $N=40$, $Z=2$ ohms, pf$=0.8$, $I_2=84$ amperes. $E_2/N=I_2Z/N=4.2$ volts per turn, giving point B on locus. $N^2/Z=800$, giving point A on 0.8 pf line. Ratio $(1/1)=AB/AO=1.19$, $\theta=\angle OAB=9$ degrees 30 minutes

Equation 11 can also be written as

$$-R(1/1)=\frac{\dfrac{N^2}{Z}+Y_1}{\dfrac{N^2}{Z}} \tag{12}$$

The construction of the complex ratio R in accordance with 12 is developed in Figure 6 where point O represents the origin of the Y_1 plane, B the end point of the admittance vector corresponding to a given value E_2/N, and AO the complex vector N^2/Z with its end point shifted to the beginning of the vector Y_1 in order to bring about the addition.

The absolute value of the ratio is equal to $R(1/1)=AB/AO$

The phase angle is equal to $\theta=\angle OAB$

If the transformer has a single turn primary, the ratio according to (5) is

$$R(N)=N\times R(1/1)$$

Using the construction shown in Figure 6, the chart in Figure 7 has been prepared from which ratio and phase angle for various burdens, secondary turns, and secondary currents can be quickly determined.

Assume a secondary current I_2, a secondary burden Z in ohms of power factor pf, and a given number of secondary turns N, the procedure followed is

1. Compute volts per turn $=I_2Z/N$ and mark point on locus Y_1 corresponding to this value as B.
2. Compute N^2/Z and mark corresponding point A at proper power factor resulting in

True ratio $=N\times AB/AO$

Phase angle $=\angle OAB$

Except for low ratio bushing-type transformers, Figure 7 can be modified so that the ratio error and phase angle can be read directly off scales. For bushing-type transformers with a large number of secondary turns and for wound-type transformers, the phase angle θ becomes so small that I_2 and I_1 can be considered parallel. Referring to Figure 2, the projection OH of OB upon the secondary-current axis divided by the secondary

Table I. Formulas for Transformer Performance

Line	Quantity	Flux-Density Range Low 100–5,000 Gausses	 High 12,000–18,000 Gausses
1	Watt/lb	$7\times10^{-9}B^2$	$5\times10^{-9}B^2$
2	RVA/lb	$3.5\times10^{-7}B^{1.6}$	$5\times10^{-41}B^{10}$
3	G_N mhos	$20.6/N^2$	$14.7/N^2$
4	B_N mhos	$\dfrac{40.2(E/N)^{-0.4}}{N^2}$	$\dfrac{2.24\times10^{-3}(E/N)^6}{N^2}$
5	$\Delta R(1/1)$	$34.8(Z/N^2)\{(E_2/N)^{-0.4}+0.296\}$	$1.94\times10^{-3}(Z/N^2)\{(E_2/N)^6+3{,}790\}$
6	θ(min)	$69{,}100(Z/N^2)\{(E_2/N)^{-0.4}-0.888\}$	$3.85(Z/N^2)\{(E_2/N)^6-11{,}370\}$

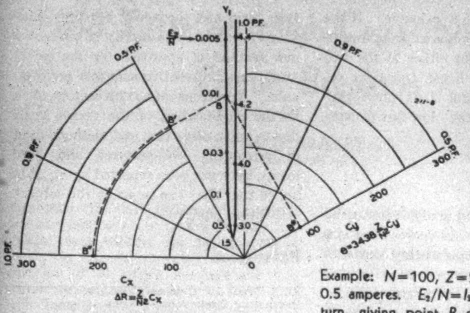


Figure 8 (left). Chart giving ratio error and phase angle for any number of secondary turns N, any total burden Z at power factors of 1.0, 0.9, and 0.5 and any desired secondary current I_1, provided $N^2/Z \gg 1{,}000$

Example: $N=100$, $Z=2$ ohms, pf$=0.5$, $I_2=0.5$ amperes. $E_2/N=I_2Z/N=0.01$ volts per turn, giving point B on locus. Project B perpendicularly upon 0.5 pf line in left quadrant giving B'; follow B' along circle about O to B'' and read value $C_x=203$, giving: $\Delta R=Z/N^2\times 203=0.04$, resulting in $R(1/1)=1.04$ and $R(N)=104$

Project B perpendicularly upon 0.5 pf line in right quadrant giving B^*; read value $Cy=88$, giving: $\theta=3{,}438\times Z/N^2\times 88=60$ minutes

current closely approximates the ratio error $\Delta R=R(1/1)-1$. Similarly, the projection, OK of OB, is directly proportional to the phase-angle. Figure 8 shows the transformation of Figure 7 for the case of "small errors" for burden power factors of 0.5, 0.9, and 1.0. The readings obtained from the scales have to be multiplied by Z/N^2. Since Figures 4, 7, and 8 are based on the Y_1 curve, they can, if desired, be combined into one chart providing all the essential data required for the following calculations:

1. Exciting current of secondary winding.
2. Ratio error and phase angle for few secondary turns.
3. Ratio error and phase angle for a large number of turns.

It is believed that these charts give a more functional picture of the transformer performance than the large number of ratio and phase-angle curves previously required to serve the same purpose. Essentially there exists only one curve, the admittance-vector locus of the core into which all other curves revert by proper transformation. The practical advantage of the Figures 7 and 8 are numerous. In the past relay engineers, when determining errors for a certain burden were forced to interpolate from a large number of curves representing different burdens and power factors. The new methods give accurate values from one single curve which gives equal emphasis to the normal load and overload range. The problem of matching transformers becomes particularly simple; ideal matching requires point A on two transformers to slide simultaneously over identical E_2/N values.

In the case of bushing-type current transformers used for overcurrent relaying it is not important to know the phase angle. Figure 8 can, therefore, be changed to permit the use of a linear scale for E_2/N as shown in Figure 9, which indicates how the Y_1 curve is transformed into the conventional ratio-error curve "*a*." It follows that for transformers with "small errors" the ratio error ΔR for any number of turns and for any burden at a given power factor and any secondary current can be represented by a single curve in a rectangular co-ordinate system with linearly progressing secondary current scale.

It is often preferable to know the ratio error in terms of primary rather than secondary current. From curve "*a*" Figure 9, which shows the ratio error in function of secondary volts per turn, I_2Z/N, and therefore of I_2, another curve can be constructed which represents the error in function of the primary current. Since $I_1=(1+\Delta R)I_2$, the following relation exists:

$$I_1Z/N=(1+\Delta R)I_2Z/N$$

Curve "*b*" in Figure 10 is obtained from curve "*a*" by determining I_1Z/N for various I_2Z/N values in accordance with the above relation and assigning to the I_1Z/N values so obtained ordinates equal to those corresponding to the respective I_2Z/N or E_2/N values.

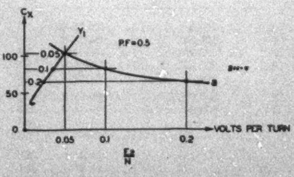


Figure 9. Transformation of Y_1 curve into conventional ratio-error curve "a", total burden power factor=0.5

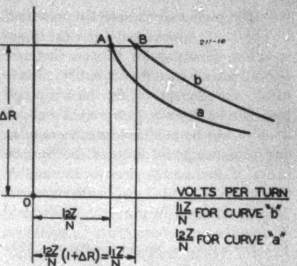

A
B
211-10
b
a
ΔR
O
VOLTS PER TURN
$\frac{I_1Z}{N}$ FOR CURVE "b"
$\frac{I_2Z}{N}$ FOR CURVE "a"
$\frac{I_2Z}{N}$
$\frac{I_2Z}{N}(1+\Delta R)=\frac{I_1Z}{N}$

Figure 10. Transformation of I_2Z/N abscissa into I_1Z/N abscissa giving curve "b" from which ratio errors at given primary current can be read

Different curves result for differing Z/N^2 values; however, since, in standard multiratio transformers with burdens of 0.5, 1.0, and 2 ohms, several combinations of burdens and turns give identical Z/N^2 values, many of the curves coincide, resulting in a corresponding reduction in the total number of curves previously required.

In Figures 7 to 10, Z designates the total secondary impedance. However, since, in bushing-type transformers with equally distributed secondary winding, the leakage reactance is negligible and the resistance of the winding is small when compared to the resistance of the external burden, Z closely approximates the external-burden impedance.

The results obtained by the use of these charts are identical with those obtained by previous methods. For any current value, any secondary burden and secondary number of turns, ratio and phase-angle values calculated from these charts are identical with those obtained by the use of previous methods.[1–4]

Part II

Referring to Figure 5 it can be seen that the admittance-vector locus consists essentially of two straight lines which are parallel to the imaginary axis, indicating constant conductance for both low- and high-density range. It has been shown that the reactive volt-amperes in these two ranges can be closely approximated by formulas containing constant exponents for the flux density B.[5] Lines 1 and 2 in Table I, give the formulas for watts/pound and reactive volt-amperes/

pound applying to the steel represented by Figure 3. Lines 3 and 4 give the corresponding formulas for the conductance G_N and susceptance B_N respectively for N turns, for the particular bushing-type transformer described above. Formulas 3 and 4 can be modified to express ratio and phase angle in terms of the components of the admittance vector, and if the study is limited to small errors or $N^2/Z \gg 1{,}000$, result in

$$R(1/1) = 1 + Z(B_N \sin \phi + G_N \cos \phi) \quad (13)$$

$$\theta_{(\text{min.})} = 3{,}438Z(B_N \cos \phi - G_N \sin \phi) \quad (14)$$

Substituting for G_N and B_N the values from lines 3 and 4 of Table I into equations 13 and 14 and limiting the investigation to the commonly used power factor of 0.5, lines 5 and 6 result for ratio error and phase angle respectively. Although a wide range of flux density is omitted from formulation, it can be seen from Figure 5 that it represents a relatively small absolute range around the bend of the admittance-vector locus. A fair approximation can be obtained for this range by assigning it constant values corresponding to the values resulting from $B = 5{,}000$ in the low-flux density formulas of Table I, lines 3 to 6 inclusive. If the expression E_2/N is eliminated from formulas, lines 5 and 6, for either of the two flux-density ranges, linear equations result between ΔR and θ as previously pointed out for the low-flux-density range.[6]

Conclusions

From the foregoing analysis it can be seen that the admittance-vector locus is a useful means of representing current-transformer performance. It is advantageous to introduce volts per turn as independent variable. This procedure results in charts from which normal and overload performance of a particular transformer at any burden and turn ratio can be obtained from one single curve, the admittance-vector locus of the steel used in the core.

A considerable saving in drafting effort is accomplished in case of multiratio bushing-type transformers where heretofore a large number of curves was required. If desired, reference to charts can be avoided by the use of simple formulas giving ratio error and phase angle for the entire load range and for any desired turn ratio and secondary burden. This study is intended primarily to present a new method of approach for the representation of current-transformer performance. Numerous important factors, such as the modification of the vector locus due to wave distortion and methods used in measuring volt-amperes and core losses, have not been reported upon; it is hoped that they can be made the subject of a future publication.

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Field Tests on High-Capacity Station Circuit Breakers

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Synopsis: The development of high-capacity air-blast breakers in this country has taken place without the background of operating experience which has attended the use of oil circuit breakers and has, therefore, emphasized the need for testing this equipment at levels comparable with the assigned interrupting ratings.

Although field short-circuit tests to verify circuit-breaker performance have been made on operating systems previously, they have been limited in most instances to moderate duty levels or, in the larger interrupting ratings, to the testing of high-voltage apparatus. Staging severe short-circuit tests at 15 kv introduces operating problems of greater significance since it virtually requires the application of the fault to the generating-station bus. This paper presents:

1. A résumé of the studies which indicated that it would be practicable to stage short-circuit tests ranging up to 2,000 megavolt-amperes directly on a particular generating-station bus.
2. A description of the physical plant equipment assembled for test purpose.
3. Operating experience during a series of 14 short circuits ranging from 200 to over 1,500 megavolt-amperes at 14.5 kv.

IN 1939 a trial installation of two 15-kv air-blast circuit breakers with a rated interrupting capacity of 500 megavolt-amperes was placed in service at the Waterside II station of the Consolidated Edison Company of New York, Inc. As a result of this operating experience a total of 24 15-kv air-blast circuit breakers ranging in rated interrupting capacity from 500 to 2,500 megavolt-amperes were purchased for installation in the Sherman Creek station.

The success with which the manufacturers of oil circuit breakers had been able to predict the performance of this equipment, as the result of field tests and extrapolation of laboratory tests at lower levels of duty, had been generally satisfactory, and there was, therefore, reasonable expectation that the same procedure could be extended to the air-blast design. However, the guide posts were not as well-marked as those in the oil-circuit-breaker field, and it seemed desirable to make tests up to the full rated interrupting capacity on one of the air-blast circuit breakers in the higher interrupting range.

When confronted with a serious proposal to stage not one but a series of major short-circuit field tests, it was only natural that the initial reaction of the more conservative minded should be one of considerable concern. The more progressive souls decreed that in addition to making an investigation of the feasibility of staging field short-circuit tests, consideration should also be given to the practicability of exposing the entire system to the short-circuit disturbance, on the hypothesis that it would be better to learn the effects under controlled conditions than possibly to be required to encounter a similar disturbance unexpectedly. The information obtained as a result of the field tests proved the wisdom of this course of action.

Preliminary Analysis

The first approach to this undertaking for making tests ranging up to 2,000 megavolt-amperes was to select the generating plant best adapted to the purpose with regard to capacity in generators, tie feeders, switching flexibility, space available for test apparatus, and facilities necessary to supply the required current and voltage. It was quickly ascertained that the Hell Gate station was outstanding in all the desired requirements by comparison with other stations in the system. Preliminary studies indicated that the mechanical stress imposed upon the windings of any generator would be not more than 50 to 60 per cent of that normally obtained with a short circuit on the generator terminals and should, therefore, be within safe limits if all winding insulation and bracing were in first-class condition. Furthermore, machines in this station had been subjected to short circuits in which the calculated mechanical stresses were of the same order as those expected in the proposed tests, and these had resulted in no apparent damage.

The second step involved short-circuit studies to determine the number of machines and tie feeders which would be necessary to furnish the required test current at each test level, making such allotment of generators and tie feeders as to expose each machine to a minimum number of short circuits. It was concluded that the three-phase short-circuit tests should be undertaken with the necessary generators isolated from the system load, except through such tie feeders as might be necessary for loading machines prior to the application of the fault and for providing the additional capacity to furnish the required fault current. This decision was based on two factors:

> First, a three-phase fault directly on the Hell Gate load bus would result in a serious disturbance to the low-voltage network system served directly from that bus at generator voltage, and this procedure could not, therefore, be undertaken without annoyance to customers.
>
> Second, the effect on the system would be greatly diminished by the cushioning impedance of the tie feeders. Moreover this would be a more severe test of the system performance than disturbances which had

Table I. Calculated and Actual Test Currents at 14.5 Kv

			Three-Phase Test Current and Duration (60-Cycle Base)				Connected Megavolt-Ampere Capacity	
			Calculated		Test			
Test	Date	Duty	Amperes	Cycles	Amperes*	Cycles	Generators	Ties
1	9-21-40	*O*	8,900	6	9,400	6		60
2	9-21-40	*O*	24,000	6	22,000	6	106	60
2*A*	9-21-40	*CO*	24,000	6	25,000	5	106	60
3*A*	9-21-40	*CO*	40,000	6	34,000	28	188	60
3	9-22-40	*O*	40,000	6	36,000	6.5	188	60
4	9-22-40	*O*	53,000	6	45,000	6.5	294	60
5*B*	9-22-40	*O*	80,000	2.4	65,000**	26.0	294	160
1*A*	5-10-41	*O*			24,000	6.2	106	60
2*A*	5-10-41	*CO*			23,000	6.2	106	60
3*B*	5-10-41	*O*			48,000	6.1	294	60
4*B*	5-10-41	*CO*			47,000	6.2	294	60
5*C*	5-10-41	*O*			62,000	3.3	294	160
6*C*	5-10-41	*CO*			59,000	5.5	294	160
7*D*	5-11-41	*O*	32,000†	12.	26,000†	14.5	188	60

* Initial arc current, highest phase. ** Current after 2.4 cycles. † Line-to-ground test.

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been encountered previously as a result of a three-phase operating failure just outside of a tie-feeder reactor and it would, therefore, afford a measure of system performance under severe fault conditions.

Since the maximum current tests were the determining factor governing the stresses on station equipment, the power requirements for this case were first determined. It was found that to attain the desired level of 2,000 megavolt-amperes, it would be necessary to raise the test voltage from the nominal operating value of 13.6 kv to 14.5 kv and to use all of the machines that could be spared from service while still maintaining the desired reserve. This required the use of a 60-megavolt-ampere tie feeder to Waterside Station II, a 100-megavolt-ampere tie feeder to the Niagara-Hudson system and three generator units of 43.75-, 62.5- and 188-megavolt-ampere capacity. In addition, it was found necessary to pretrip the test breaker in order to take advantage of the current asymmetry. The calculated currents together with the necessary facilities in machines and tie feeders for the various tests are shown in Table I.

A complete analysis of short-circuit forces was made on the connections and equipment of the feeder position supplying the test breaker. All calculations were based on the maximum peak value of current expected under the highest capacity test. Full offset at "zero" time was assumed and proper allowance made for the decay of both a-c and d-c components, resulting in a maximum instantaneous peak current, at the first half-cycle following the fault, of 200,000 amperes at 14.5 kv.

Since the station buses and feeder outlets are of isolated phase arrangement, forces arising from the interaction of phase currents are of little or no importance until the several phase conductors converge in the station cable vault. The arrangement of equipment grounding coppers within the isolated phase portion of the electrical galleries was such that any possible failure to ground would require ground-current flow at right angles to the phase conductors. This eliminated the necessity of considering interaction of phase and ground-fault current. Consequently, within the galleries, the actual forces considered were only those on the test-feeder circuit, arising from the current flow in the several sections of the phase conductor, as it looped, turned, and doubled back on itself, in passing through oil circuit breakers and the reactor of its own phase.

The maximum force concentration appeared as a cantilever load on the wall bushings entering the oil circuit breaker compartments. This amounted to approximately 5,000 pounds at the end of the bushing and normal to its axis. A greater force, 6,000 pounds, was found to act on the oil-circuit-breaker moving contact, tending to open the breaker against the restraint of its latching mechanism.

Since the wall bushing mentioned above was rated by its manufacturer as able to withstand only about 600 pounds at the end of the stud, it was decided to eliminate these forces by placing a short-circuiting strap across the external bushing studs, removing the circuit breakers and reactors of the feeder from the circuit, and thus eliminating virtually all cantilever loading on the wall bushings.

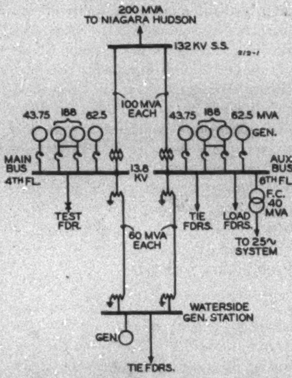


Figure 1. Simplified diagram of system connections to Hell Gate station in the maximum-capacity test

Within the station cable vault, where the separate phase conductors making their exit from conduit were racked in a parallel flat arrangement on the wall of the vault, maximum instantaneous forces of 3,700 pounds per foot were found. In regions where the cables converged to enter a joint with three-conductor cable, the sheaths were in contact, and the maximum instantaneous value of the forces tending to separate the cables was found to be 11,500 pounds per foot. Suitable wrapping and bracing were provided to hold these cables securely in place during the short circuit. Disconnecting switches in the test circuit, where rated below the expected test current, were removed and replaced by copper bus work.

Concurrently with the foregoing studies, an investigation was undertaken to determine the likelihood of instability between generators following clearance of the trouble. It was assumed here that the fault might have to be cleared by the backup protection, and that the total clearing time would be approximately 0.5 second. These calculations indicated that there would be no question whatever of stability, since no generator should swing from its initial position relative to the others by more than 10 degrees. Furthermore, the load readjustment throughout the system, after fault clearing, should not be sufficient to result in any material disturbance, even though the Waterside tie feeder should trip. The calculated voltage dips during the highest capacity fault were 18 per cent on the Hell Gate load busses, 15 per cent at Waterside II, 20 per cent at Sherman Creek, and 40 per cent at the Dunwoodie substation. A simplified one-line diagram of the 60-cycle system tie connections is shown in Figure 1.

Organization

A working committee was set up, comprising representatives of the production, system operation, construction, technical service, and electrical engineering departments of the company and also representatives of the General Electric Company. Schedules and dates were set up to cover:

A. Drawing and engineering information.

B. Construction dates and equipment deliveries.

C. Test procedure.

D. Personnel assignments.

To a key man in each group was delegated the responsibility of executing the work assigned by the chairman of the working committee. Through this organization it was possible to keep all work moving on schedule and settle all of the points, with a minimum of delay and with complete understanding as to the action to be taken by all concerned. This procedure was responsible for the success which was attained in completing all preparations on schedule and eliminating confusion as to procedures and responsibilities, during the actual carrying out of the test work.

Test Facilities

The breaker selected for test was a General Electric Company type *A R*-20-150 three-phase 60-cycle 1,200-ampere 15-kv unit with a rated interrupting capacity of 1,500 megavolt-amperes. The detailed description of this circuit breaker is covered in a companion paper,[1] and its

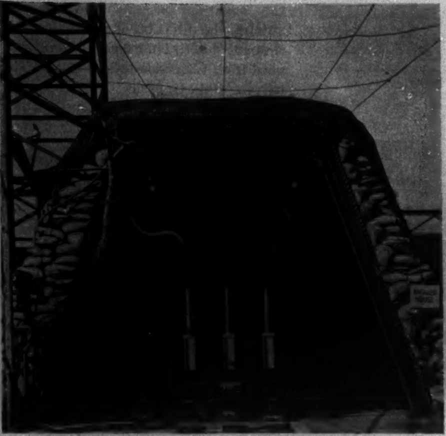
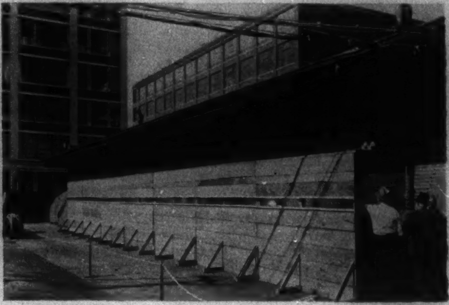


Figure 2 (left). Air-blast circuit breaker installed in structure

Control wiring and instrument wires to control station shown at upper left corner of structure

Figure 3 (below). View of observation barricade

Control cables from circuit breaker may be noted at the top



performance will be referred to here only insofar as it was concerned with the testing operations.

One of the type *H* circuit breakers used for closing duty in the testing station of the General Electric Company was used for the same duty in these tests. This breaker was installed in the breaker test house in close proximity to the air-blast breaker.

The clearance of material stored in the transformer and cable yard to make space available for the breaker test cubicle and other structures, at first appeared to present a task of gargantuan proportions, what with scores of cable reels, economizer tubes, grate bars, and an indescribable assortment of material accumulated by a decade of "string savers." At the very beginning, therefore, the test program began to pay dividends, to the delight of the station superintendent. It made necessary a general house cleaning of the storage yard, by transfer of some of the equipment to other locations, and a more orderly restacking of the remainder.

The breaker house structure, consisting of a steel framework closed on all three sides and the top with corrugated steel sheeting, was supported on a concrete mat. All enclosing sides and the top of the structure were covered with two layers of sandbags, as a protection to numerous transformers and other equipment against damage from possible fire and flying debris.

The open side of the structure was laid out to face a one-story brick storeroom structure. A barricade of two-inch thick lumber, erected in ten-foot sections for quick assembly and removal, for protection of the observers, was placed under the overhanging roof of the storeroom, directly facing and approximately 70 feet distant from the breaker house. A space in the storeroom, with a separate entrance door facing the breaker house, provided a convenient area for all of the magnetic oscillograph equipment required by the General Electric Company. The control station, for operation of the closing breaker and test breaker, as well as the communication facilities, with the generating station control room, was also located at this point. The remainder of the equipment required for the test included separate, standard wood construction shanties, such as were readily available in the market. Four such structures were used for housing the field construction office, dark room, air compressors, and cathode-ray oscillograph. Photographs of the breaker test house and the observation barricade are shown in Figures 2 and 3 respectively.

An independent three-point communication system was installed to provide communication between the test-control station in the yard, the generating-station control board, and the generating-station control-cable terminal room, where was located the Company's automatic recording instruments. A public address system was also installed, for information of the observers concerning safety regulations, announcement of test procedures, and the results of the various tests.

A standard feeder outlet position was used for the test outlet from the station bus, and the circuit from this point to the breaker test house consisted of two three-conductor 800,000-circular-mil lead-covered cables in parallel.

After careful consideration, the decision was reached that the station breakers should be used for backup protection against failure of the test air-blast breaker since there was some element of risk in using the control breaker in the breaker test house, due to its close physical proximity to the test breaker, and furthermore the control breaker might also fail as a result of its use in applying short circuits. The bus setup in the station could be arranged so that two 5,000-ampere, 1,500-megavolt-ampere bus-tie oil circuit breakers, in the section of bus to which the test feeder was connected, would divide the total test current, so that neither one would be subjected to a duty in excess of two-thirds of its rated interrupted capacity. Current transformers, installed on the bus section to which the test feeder was connected, were disconnected from the bus-differential relay circuit and connected, in such a manner as to totalize the total test current to a backup overcurrent induction relay, arranged to trip the two bus-tie oil circuit breakers. These connections are shown in Figure 4.

Operating Procedures

In order to maintain suitable safeguards and to avoid confusion and misunderstanding, the following operating procedure was established:

A. A production department representative with five assistants was stationed in the test area. The operator in charge of this group was responsible for the enforcement of safety regulations. He saw that the test area was cleared, protective barriers were in place, and so on, before each test. He also issued and cleared the necessary work permits for inspection of the test breaker after each test and stationed his watchmen at various points in the test area before each test. This operator alone gave all of the orders to the high-voltage operator in the station for energizing and de-energizing the test feeder. Thus by delegating this authority to only one individual, any confliction of orders was avoided.

B. In the electrical galleries the doors between adjacent bus sections were blocked open, and qualified observers stationed in the gallery corridors on all floors had an unobstructed view of the gallery compartments during test periods. This procedure provided a means of detecting the nature and location of any trouble and also prevented the unauthorized access of other personnel.

C. A representative of the steam engineering department was stationed at the throttle of each turbine, and these men were under instructions as to the action that they should take in the event of trouble developing either on the turbine or the generator. The station electrical mechanics maintained a watch on the generator, and the generator cable ducts, and neutral cable duct runs, to watch for any indication of trouble in those areas.

D. A simple signal system was set up to warn all concerned that a test was about to take place. Signals were given throughout the generating station ten minutes in advance of each test. Each inspector or observer then reported by telephone to the high-voltage control board that he was in position. Five minutes before each test, a signal was given on the turbine-room call whistle and a "standby" signal put up on the signal stand on each turbine being used for the test. When all observers were reported as being in position, the production department representative in the test yard was notified and then proceeded to give the necessary switching instructions, relative to making the test feeder alive. One minute before each test communication was established between the production representative in the yard, the high-voltage operator, and the representatives in charge of the test instruments set up in the cable-terminal control room. Time was then counted off, and the test applied. Communication was maintained until all observers had reported to the high-voltage operator. At the high-voltage operating board an operator was stationed at the controls of each generator and was under instructions as to the steps which should be followed should trouble develop. Observers were also stationed at each street manhole through which the test feeder was carried, all manhole covers being removed during the test period.

F. An electrical construction crew was maintained in the station construction office for any necessary work which might develop as a result of unexpected trouble. For the same purposes a crew from the underground department was also maintained during the entire test period, to be available for any necessary repair work which might be required.

G. Beginning with the test at the 1,000-megavolt-ampere level and above, inspections were made of the equipment in the electrical galleries, the station cable bay, and the street manholes, for evidence of damage after the first test at each level.

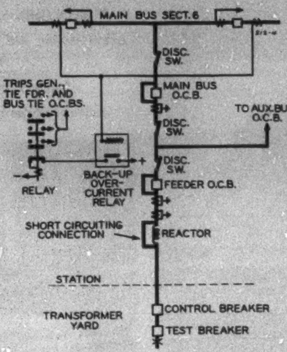


Figure 4. Diagram of relay connections for backup protection

1940 Tests

The first series of tests was undertaken Saturday, September 21, and the following Sunday, September 22, 1940. Test 1 at 200 megavolt-amperes was in the nature of a trial test shot for checking all of the equipment and the operating procedure. In this test the Waterside II tie feeder was tripped at Waterside number 2 by the directional impedance relays. This was the first of the unanticipated events. Later inspection of the oscillographic recording disclosed that the relay operation was undoubtedly due to the asymmetry of the current, which had the effect of increasing the operating range of the relay by some 40 per cent. This relay was blocked on subsequent tests, but protection was maintained by backup time delay over current relays.

On test 2 at 550 megavolt-amperes the same tie feeder was tripped again, but, on this occasion, by the directional impedance relay at the Hell Gate end of the feeder. This was unanticipated operation number 2. This relay was also blocked out for succeeding tests. Subsequent tests on this relay disclosed that there was excessive "wipe" in the contacts of the directional element. The incorrect operation was, therefore, attributed to failure of the directional contact to open before the impedance contacts closed. This may be better understood when it is noted that prior to the fault the power flow on the tie feeder was from Hell Gate towards Waterside, the tie feeder being used to load the generator prior to the test. The directional elements of the impedance relay were, therefore, closed under this condition, but when the fault was applied, the direction of power flow was reversed due to fault current being supplied from Waterside to Hell Gate. The tripping of the tie feeder, of course, caused a loss of load to the two generators after clearing the fault. The machines were, however, resynchronized to the station load bus without difficulty.

Test 2A at 550 megavolt-amperes was run off without undue incident.

In setting up the busses for the next test, a 160-megawatt unit, which had not been used in any of the tests up to that time, was placed on the test bus. One of the gallery observers heard a heavy static discharge, which appeared to come from the vicinity of the generator breaker compartment. This was unexpected event number 3. Although not directly related to the breaker test, this condition might have resulted in an operating failure if it had not been for the observers posted in the electrical galleries. The machine was immediately removed from service and testing of the circuit undertaken. It was ascertained that one of the stress cones at the termination of one of the cables was the seat of the trouble. The cable terminal was remade and gave no further trouble.

On test 3 at approximately 800 megavolt-amperes the air-blast breaker arced over on two phases and failed to clear the fault. The fault was cleared by the backup bus-tie breakers in the station in 29.5 cycles. Subsequent inspection of the bus-tie breakers disclosed that the 6-7 section bus-tie breaker had thrown some oil on the *B* phase pole unit. While the performance of the bus-tie breakers was not unusual in this respect, it was considered that a little more discretion might profitably be exercised, as means were available for so doing. The psychological effect of the flash and the accompanying sound effects, in connection with the arc to ground at the test breaker, undoubtedly lent some emphasis to the need for more caution. As a consequence, the backup relay in the generating station was arranged to trip all generators and tie feeders connected to the test bus, as well as the bus-tie breakers, since this would operate to distribute the duty among the various circuit breakers in the station somewhat more equitably.

It is well worthy of note that, although the air-blast breaker was subjected to severe arcing for nearly one-half second, there was no ensuing fire after arc extinction, no damage to any of the immediately adjacent apparatus in the breaker house, and the necessary servicing and adjustment were completed in approximately two hours. This performance may be contrasted with that commonly experienced with oil-circuit-breaker failure of like nature.

After repairs and adjustments to the air-blast breaker, test 3 at approximately 860 megavolt-amperes and test 4 at approximately 1,000 megavolt-amperes were completed without undue incident.

On test 5*B*, at approximately 1,500 megavolt-amperes, the air-blast breaker again failed to clear the fault, and it was cleared by the backup protection in 26 cycles. The damage to the air-blast circuit breaker was confined principally to the glaze of interphase brazing insulators and dislocation of some parts of the arc chute. As in test 3*A*, there was no damage to adjacent equipment. There was no evidence of stress on any of the station circuit breakers except to a minor degree on the 6-7 bus-tie breakers. The voltage dip on the network distribution system ranged from 15 to 30 per cent in the Manhattan and Bronx districts, approximately 50 per cent at the Dunwoodie substation, and 10 to 20 per cent on the Niagara-Hudson system. A one-tenth cycle variation in frequency was recorded. A number of railroad synchronous converters in the Westchester area tripped out as a result of the disturbance. There were no operations of automatic network protector units noted at 24 key observation points. All generators and tie feeders were restored to the system within $4^1/_2$ minutes.

An examination of the end windings of all generators and equipment in the electrical galleries following these tests disclosed absolutely no evidence of any damage to the equipment.

1941 Tests

The second series of tests was made on Saturday, May 10, and Sunday, May 11, in 1941. The preparation was comparatively simple, since all the facilities had been retained from the previous year. A total of six three-phase short-circuit tests and one single-phase short-circuit test was scheduled. The three-phase tests consisted of one "*O*" and one "*CO*" test at approximately 600-, 1,200- and 1,500-megavolt-ampere levels.

The breaker tested in this series was the same as that used previously, except for modifications to increase the air pressure from 150 to 250 pounds per square inch, and certain modifications in the design of the arcing chamber and the exhaust stack. A special air-blast breaker designated as type *AR*-20-250*Y*, three-pole, 15-kv, was used for the closing control breaker in this series of tests, in lieu of type *H* breaker used previously. An additional cathode-ray oscillograph was provided for measurement of the voltages from the short-circuiting connection on the test breaker to ground.

All of the short-circuit tests were successfully interrupted by the air-blast breaker, and, as a consequence, all test work was completed very close to the schedule. During the tests at the higher levels of duty, there were several reports of flashes and sparks in the electrical galleries in the generating plant. These were, naturally, a matter of some concern, until a careful inspection disclosed only minor burns on the circuit-breaker operating rods at the clevis pins on the third floor and between a lighting-fixture outlet box and a ground bus on the second floor. As the short-circuit connection on the test breaker was ungrounded, and there was no indication of system ground current, it was evident that these manifestations in the galleries could have been caused only by induced voltages set up by the test current. Additional clearance was established at the points concerned and no further reports of sparks were received.

It is of interest to note that, on tests at the 1,500-megavolt-ampere level, the railroad synchronous converters in the Westchester area, which were subjected to a 50 per cent voltage dip, did not trip out as in the tests made the previous year. This very clearly indicates the benefits of high-speed fault clearing with respect to its effect on operating equipment.

In the "*O*" test "5*C*", it was hoped that, by setting the pretrip relay to operate 1.2 cycles after initiation of the fault, it might be possible to increase the test duty over that obtained in the previous year. For some unexplained reason, this was not entirely successful, although the pretripping time was checked by tests, before the test current was applied. The oscillographic record shows that arcing did not start until nearly three cycles after initiation of the fault, rather than 1.2 cycles as anticipated.

It may also be noted that the actual current obtained on tests was appreciably lower than the calculated values, particularly in the tests in excess of 1,000 megavolt-amperes.

This discrepancy may be attributed to two factors:

1. The reactance of the generator cables, busses, and test circuit within the station was not included in the calculations.
2. The subtransient and transient reactance constants of the large generator (188 megavolt-amperes) were probably higher than the calculated constants.

The line-to-ground test (7*D*) was made for the purpose of obtaining test data on the zero phase sequence reactance of the 188,000-megavolt-ampere unit and the station grounding system, rather than for testing the air-blast breaker. The 188-megavolt-ampere generator is composed of two 94-megavolt ampere generators driven by a cross compound turbine. The neutral of only one of the generators is grounded. In this test this generator unit and the tie to the Waterside II station were isolated from the rest of the system. The breaker tripping time was intentionally delayed in order to obtain a steady-state value of current. While the measured values were lower than those which had been calculated,[1] this was attributed to the reactance introduced by the various multiple paths over which the ground current could flow between the generator neutral connection and the termination of the test cable in the electrical galleries. A recalculation of this circuit has shown that the difference between the actual test current and the calculated current would be accounted for by 0.05 ohm.

Conclusions

1. High-capacity short-circuit tests directly on the bus of a large power station may be made without unreasonable risk to the system and equipment, if the undertaking is preceded by careful planning, and if safeguards are established to protect against the abnormal conditions which customarily prevail as the result of short-circuit phenomena.
2. System tests under controlled conditions furnish valuable data on system and equipment performance. Such tests not only serve to check the performance of the apparatus ostensibly under test but also demonstrate the adequacy of other parts of the system, under the most severe conditions they are designed to meet.
3. Calculations of generating-station bus faults, in which the reactance of even very short runs of connections are neglected, apparently result in currents which are appreciably higher than actual values and are, therefore, on the conservative side in determining the interrupting duty of switching equipment.
4. Field testing tends to instill more confidence in the science of stability computations which has contributed so materially to the establishment of sound engineering principles in the design and operation of power systems.
5. It has been realistically demonstrated that the fire hazard inherent in the oil-circuit-breaker design is greatly minimized by the air-blast design.

Reference

1. FIELD TESTS ON HIGH-CAPACITY AIR-BLAST STATION TYPE CIRCUIT BREAKERS, H. E. Strang and W. F. Skeats. Scheduled for AIEE TRANSACTIONS, volume 61, 1942 (February section).

Frequency-Modulated Carrier Telegraph System

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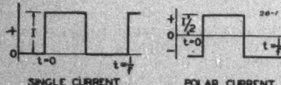
Synopsis: Voice-frequency carrier-current telegraph systems used in this country and abroad have until now employed amplitude modulation, analogous to single current signalling in d-c telegraphy. A variety of so-called two-tone telegraph systems has been tried out by various workers, but none of these was adopted on a commercial scale because all required the employment of at least double the frequency spectrum space ordinarily assigned for amplitude modulation. The system described employs true frequency modulation to derive the advantages of polar current signalling, with the same spectrum efficiency as conventional amplitude systems, and secures at the same time freedom from attenuation change in the transmission medium and greater immunity to extraneous disturbing currents.

FREQUENCY-modulated systems for radiobroadcasting and facsimile transmission have been developed and placed in operation with remarkable success during the past few years. Publications covering practically all phases of this type of modulation, particularly with reference to its interference suppression qualities, have been legion. The fundamental principles have been soundly established and today are perhaps as thoroughly understood as those of amplitude modulation. Consequently, this paper will be confined to a brief outline of recent land-line voice-frequency telegraph-carrier developments culminating in the adaptation of frequency modulation.



SINGLE CURRENT TRANSMISSION

POLAR CURRENT TRANSMISSION

Figure 1. Relative current requirements in single- and polar-current systems for equal interference susceptibility

Theory

Virtually all telegraphic communication circuits fall under either of two basic types of operation:

(*a*). Single-current operation wherein transmission of current indicates a marking signal and absence of current a spacing signal.

(*b*). Polar operation wherein transmission of current of one sense or sign indicates a marking signal and current in the opposite sense or sign indicates a spacing signal.

Relative current requirements for equivalent interference susceptibility in single and polar operation are given in Figure 1. The resulting power relations are as follows:

$$\frac{\text{Power per cycle single operation}}{\text{Power per cycle polar operation}}=\frac{I^2R(t/2)}{(I/2)^2Rt}=2$$

$$\frac{\text{Peak power single operation}}{\text{Peak power polar operation}}=\frac{I^2R}{(I/2)^2R}=4$$

The two-to-one reduction in average power and the four-to-one reduction in peak power pertaining to the polar method constitutes a major reason for its adoption on all but relatively short d-c telegraph circuits. In addition, it is inherently immune to the bias losses experienced in single current operation as a result of variations in circuit transmission equivalent. Where peak current permissible is the limiting factor a two-to-one susceptibility gain is realized.

From their earliest inception down to the present time practically all telegraph carrier circuits have been founded on the basic form shown in Figure 2 wherein a carrier frequency is amplitude modulated, energy being transmitted on marking signals and interrupted on spacing signals in the usual single current mode of operation. The resulting square-topped wave trains existing at section *A* are modified by the restricted bandwidth of the channel tuners to the envelope form shown at section *B*. The envelope is then converted by a linear demodulator to a similarly shaped unidirectional pulsating current flowing through the receiving relay, as indicated by *C*. Faithful reproduction of the original modulation is secured by applying to the receiving relay a local bias of such magnitude as to produce equal marking and spacing intervals from the relay armature, curve *D*. It is self-evident from the sinusoidal nature of *C* that any alteration in its magnitude relative to the local operating bias—as, for example, might result from varying line attenuation—will destroy the equal time interval relationship between marking and spacing pulses introducing a loss in the form of biased relay action.

Inasmuch as the advantages of polar operation have always been clearly recognized, the development literature of the carrier telegraph has been replete with suggestions and schemes for its realization either in full or in part. To overcome bias susceptibility, for example, more or less complicated devices have been evolved which function either to maintain the demodulator input at a predetermined level or to regulate the receiving relay bias in accordance with received carrier level. Such artifices have been in use for a number of years with creditable results, their effectiveness being of the order indicated in Figure 3.

Of a more fundamental nature is the oft proposed "two-tone" polar carrier system involving two carrier sources of somewhat different frequency operating over separate channels and terminating differentially in a receiving relay as shown in Figure 4. Energy of frequency F_m is transmitted over one channel for a marking pulse and energy of frequency F_s over the other channel for a spacing pulse, thus closely approximating the requirements for polar transmission and providing the increased stability pertaining thereto. In theory, but not always in practice, both channels are similarly affected by variations in circuit attenuation, thus fulfilling the polar conditions for bias-free recep-

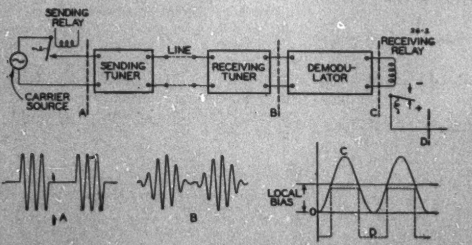


Figure 2. Fundamental carrier telegraph circuit

A—Keyed carrier
B—Received carrier
C—Relay current
D—Relay operation

Paper 42-6, recommended by the AIEE committee on communication for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted October 20, 1941; made available for printing November 3, 1941.

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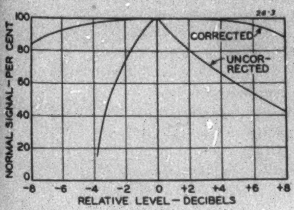
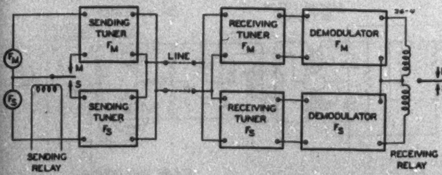


Figure 3. Effectiveness of automatic bias corrector

tion. From an operating aspect, however, this circuit is economically infeasible, requiring at least twice the frequency spectrum per telegraph channel as a single-current system. Suggestions for circumventing this obstacle usually involve a closer spacing of F_m and F_s so that both frequencies will be accepted by a tuner of the type normally provided for amplitude-modulation signalling. The fallacy herein can be seen by inspection of Figure 5, illustrating the distribution of carrier and sideband voltages in amplitude modulation of a carrier frequency.

In a single-current system (Figure 5a) the two intelligence-bearing sidebands created by the act of modulation and the carrier are symmetrically located with respect to, and lie entirely within the available band. The side bands, of course, are separated from the carrier by an interval equal to the modulating frequency. If an attempt is made to pass two carriers F_m and F_s representing marking and spacing frequencies through a similar band, the impossible conditions illustrated in Figure 5b obtain. Both carriers are amplitude modulated, each having an independent set of sidebands, the higher sideband of F_s and the lower of F_m lying outside the passband. The remaining components reaching the receiving demodulator will produce in its output not only a portion of the original modulation but also the beat between the two carriers and the two sidebands. The result will be severe distortion which can be eliminated only by sufficient separation and selection of F_m and F_s and their respective sidebands.

Figure 4. Two-tone polar carrier system employing separate channels for marking and spacing frequencies



A mathematical analysis of carrier and sideband components resulting from the sine wave frequency modulation of a carrier F_0 between the limits F_m and F_s at a rate equal to $(F_s-F_m)/2$ (unity modulation index) discloses the relative voltage relationships shown in Figure 6. A theoretically infinite number of sidebands are created symmetrically spaced with respect to the carrier and separated by intervals equal to the modulating frequency. Practically, however, the energy content of sideband components lying beyond the second is sufficiently minute to eliminate them from consideration. In addition, for telegraphic purposes, it has been found possible to further eliminate second-order sidebands without introducing serious distortion, leaving a spectrum during modulation similar to Figure 5a. The small distortion component can be eliminated by simple resistance-capacity shaping of the received signal if so desired. Under steady-state or d-c conditions the F_m or F_s frequency only is present, depending upon the transmitting relay position, and thus we have a method for deriving a "two-tone" polar system requiring no greater bandwidth than a conventional single-current amplitude-modulated carrier system. Except under certain critical conditions the F_m and F_s frequencies do not appear during modulation.

Description of System

The most obvious and practical method of frequency modulating a carrier in response to polar d-c telegraph signals is to vary the frequency of an oscillator by means of the transmitting relay. Figure 7 illustrates the method wherein the frequency determining circuit LC, tuned to mid-passband frequency F_0, is shunted by a control network comprising L_1 and L_2C_2 separately in series with rectifier elements A_1 and A_2. A cycle of operation, starting with the transmitting relay on spacing, connects positive battery to the control circuit establishing across R_1 a potential, positive with respect to ground, of magnitude slightly greater than the peak oscillating voltage existing across LC. Under this condition the impedance of rectifier A_2 approaches infinity, effectively isolating L_2C_2. Simultaneously rectifier A_1 becomes biased in the conducting direction, R_2 acting to limit the control current to a value slightly greater than the peak oscillating current flowing through A_1. As C_2 presents a low impedance to the carrier frequency, L_1 effectively parallels LC and the frequency approaches

$$\frac{1}{2\pi\sqrt{\frac{LL_1}{L+L_1}C}}=F_s$$

In like manner marking or negative battery applied by the transmitting relay isolates L_1, and L_2C_2 effectively parallels LC, the frequency approaching

$$\frac{1}{2\pi\sqrt{\frac{LL_2}{L+L_2}(C+C_2)}}=F_m$$

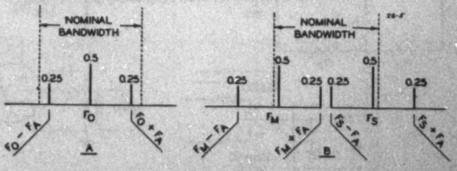
L_2 is necessary to provide a d-c path for the rectifier polarizing current similar to that provided by L_1. In practice, L_1, L_2, and C_2 are so proportioned that

$F_0-F_m=F_s-F_0=70$ cycles

The modulation index becomes equal to unity at a keying speed of 70 cycles per second, the nominal maximum of the system. The conditions set forth in Figure 6 are met by introducing a shaping network N to restrict the rate of change of control circuit voltage to an approximately sinusoidal shape at the maximum modulation frequency. Failure to provide this network introduces a distortion component of relatively small magnitude in the received signal. As might be surmised from Figure 7, the impedances of rectifiers A_1 and A_2, varying between maximum and minimum in inverse relationship, produce an amplitude factor in the modulated wave. This component is considerably smaller and, fortunately, inverse in phase to a similar component introduced by the curvature inherent in the attenuation characteristic of a normal carrier telegraph channel. Frequency

Figure 5. Carrier and side-band relationships in amplitude modulation

F_A = modulation frequency



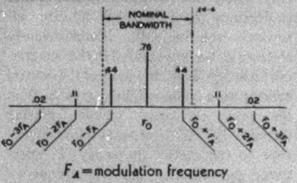


Figure 6. Carrier and side-band relationships in frequency modulation for unity modulation index

deviation accuracy of a high order is achieved as the percentage deviation is independent of voltage (above a certain critical minimum value) applied to the control circuit.

The receiving terminal employs a current limiter, converter, and differential detector as illustrated in Figure 8. The current limiter is perhaps unique in that regenerative action is utilized to secure the high order of sensitivity required on certain classes of systems where terminal repeaters are not justifiable. Regeneration is set to give stable limiting action down to minus 45 decibels, a gain in sensitivity of approximately 25 decibels over the unregenerative state. Thus a practically constant value of effective relay operating current is assured for all conditions conceivably encountered in commercial carrier practice. Conversion from frequency modulation to amplitude modulation prior to detection is effected by a discriminator comprising two parallel-tuned circuits L_1C_1 and L_2C_2 operating in series and tuned respectively to marking and spacing frequencies. The two discriminator output voltages, after being separately detected, are differentially added before being applied to a d-c power amplifier stage for operating the receiving relay. A differential biasing circuit is provided to compensate for slight circuit or relay misalignment. Figure 9 shows the mechanical arrangement employed in transmitter and receiver design, each unit occupying $3^1/_2$ inches of vertical rack space.

Comparative Test Results

A series of laboratory and field tests were made to determine the relative merits of frequency-modulated and conventional amplitude-modulated systems. The following data were taken at a line speed of 60 cycles per second under identical circuit conditions on a basis of equal peak carrier voltages as a criterion. A transmission testing machine was used to determine operating margins under any given set of conditions in terms of milliseconds loss, a method universally recognized for its accuracy in telegraph transmission studies.

A comparison of circuit susceptibility to single-frequency crosstalk (simulating severe unbalance in four-wire carrier systems as may be encountered under emergency conditions) is given in Figure 10. With amplitude modulation the signal loss is substantially proportional to the receiving tuner attenuation characteristic, becoming a maximum of 3.15 milliseconds for a 12 decibel peak ratio between signal and crosstalk at mid-band frequency. Conversely the frequency modulation characteristic displays the familiar double hump resulting from its triangular noise spectrum wherein the loss due to a single interfering frequency is proportional to its separation from the carrier frequency. As a deviation ratio of unity is employed, maximum loss occurs at a crosstalk frequency differing from the carrier by an interval equal to the deviation frequency. The signal loss at this point of greatest susceptibility is approximately 1.2 milliseconds, or a decrease of 8.3 decibels from the maximum loss with amplitude modulation for interference of this character.

A common form of undesirable noise encountered in telegraph carrier circuits is a random or fluctuating type produced by battery supplies, shot effect in amplifiers, etc. wherein the interference contains frequency components more or less uniformly distributed throughout the channel pass-band. Figure 11 is the average peak loss in milliseconds as measured at the receiving relay contacts for various values of fluctuating noise—a particularly difficult task to perform accurately owing to the probability factor inherent to random interference. A substantial improvement of roughly 9 decibels obtains to frequency modulation for large signal-to-noise ratios as compared with the theoretical value of $2\sqrt{3}\,F_d/F_a$ or 10.8 decibels.

A third form of interference to which open wire carrier circuits are particularly vulnerable is the impulsive type, usually attributable to lightning, wherein shock excitation of the receiving tuner by a steep wavefront produces in the terminal equipment an exponential wave train having a fundamental frequency in the neighborhood of the nominal carrier frequency. The ultimate signal loss is a function of both the amplitude and the phase of the transient relative to the instantaneous carrier, resulting in an interference fortuitous in nature. Peaks of maximum loss thus occur on a probability basis and must be given due consideration in the method of loss measurement. Figure 12 is a comparison of amplitude and frequency modulation under these conditions and closely resembles Figure 11 for fluctuation noise. A reduction of approximately 10.5 decibels in average peak loss is secured for large signal-to-noise ratios as compared to the theoretical improvement factor $4F_d/F_a$ or 12 decibels.

Operational Considerations

It is common practice in high-frequency carrier systems to employ group modula-

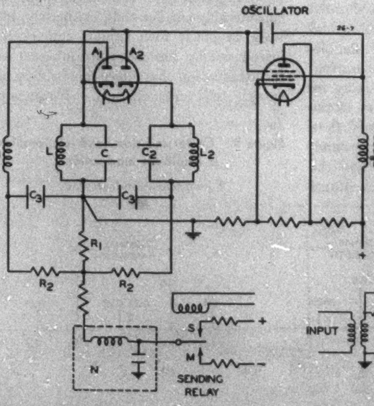


Figure 7 (left). Schematic diagram of frequency modulator

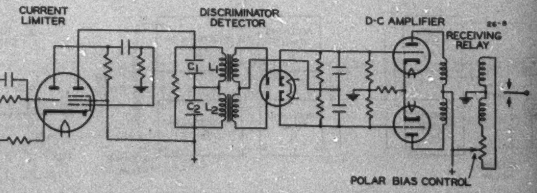


Figure 8 (below). Schematic diagram of frequency-modulation receiver

Figure 9. Frequency-modulation equipment

A—Modulator-oscillator

B—Demodulator-amplifier

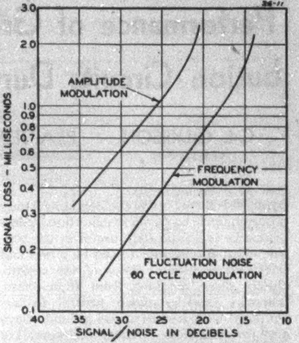


Figure 11. Effect of fluctuation noise on amplitude- and frequency-modulated systems

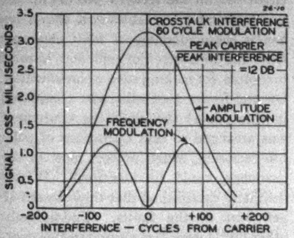


Figure 10. Effect of single-frequency cross talk on amplitude- and frequency-modulated systems

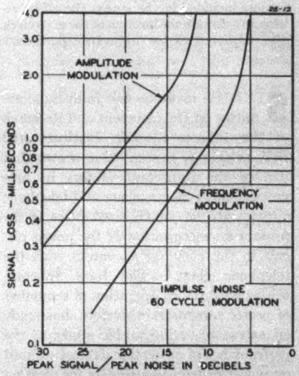


Figure 12. Effect of impulse noise on amplitude- and frequency-modulated systems

tion and demodulation for transferring the fundamental carrier channels to their assigned frequency band for transmission over the line. In the perfect case, channel frequencies leaving the receiving group demodulator are identical to those entering the transmitting group modulator. Practically, however, a frequency shift exists equal in magnitude to the instantaneous frequency difference in the carrier sources available for group modulation and demodulation which, by the very nature of frequency modulation, introduces a bias component in the received signal. It is required, therefore, that each frequency translating oscillator be a highly stable carrier source having an absolute frequency variation of less than plus or minus two cycles. Similarly, the tolerances on channel tuner stability are somewhat more stringent than for amplitude modulation but can be easily attained in production when proper precautions are observed. The difficulty of accurate level regulation on long carrier circuits is reduced to a problem of maintaining the aggregate repeater loads safely below the distortion point. This is easily accomplished by various well known techniques. Where higher grade carrier channels are required on an existing amplitude system, frequency-modulated channels can be directly substituted without disturbing the rest of the installation.

Evidence thus far presented would indicate a substantial improvement in carrier circuit performance wherever existing amplitude-modulated systems are converted to frequency modulation. This has been amply substantiated in practice during the past three years. Perhaps the outstanding advantage from a carrier attendant's viewpoint lies in the immunity of frequency modulation to received level variations. This feature provides greater flexibility as a channel group can be instantaneously swapped between routes having widely different equalization characteristics without readjusting individual channel levels for optimum performance; an important consideration where synchronism must be maintained on automatic telegraph circuits. In addition, detrimental effects of small instantaneous level fluctuations continually occurring on open wire carrier circuits are completely eliminated.

Polar operation and the triangular noise spectrum of frequency modulation combine to produce a real and substantial improvement in circuit interference susceptibility, reflected in greater operating margins and reduced testing and regulating work.

Performance of Ground-Relayed Distribution Circuits During Faults to Ground

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Synopsis: An extensive oscillographic study has been made on power distribution feeders primarily to obtain data useful in the consideration of joint use of poles by power and telephone facilities. Some of the results, chiefly those obtained from three-phase, four-wire, multigrounded neutral feeders equipped with instantaneous ground relays and for immediate breaker reclosure, are believed to be of general interest and are presented herewith. Included are data on the performance of the protective devices utilized for clearing ground faults on the feeders included in the study, the effectiveness and certain limitations of these devices, and characteristics of the faults experienced.

STUDIES made by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System have shown that one of the important factors in promoting safety where power and telephone circuits are on jointly used poles is the prompt de-energization of the power circuit in the event of a contact with the telephone plant.[1] The Joint Subcommittee with the co-operation of a number of power companies, therefore, has made an extensive oscillographic study of the performance of protective devices utilized on power distribution feeders for clearing ground faults. Some of the feeders selected for study were of the three-phase, four-wire, multigrounded neutral type equipped with instantaneous ground relays and for immediate reclosure of the breaker. This paper deals primarily with the data obtained from this type of feeder regarding the performance of the protective equipment in clearing ground faults and the characteristics of the faults experienced which are thought to be of most general interest.

Since the oscillographs usually were arranged to record only residual currents in the power feeders, multiple-phase faults were recorded only when ground was also involved. As some disturbances were self-clearing and some were due to reclosures on or recurrences of sustained faults, somewhat special meanings have been attached herein to the terms "fault" and "disturbance."

"Fault" is considered to be any occurrence which caused a measurable oscillogram of power system current, which, in so far as could be established, was not from reclosure of a breaker or fuse on a previously existing trouble, from repeated oscillograph operations on a sustained case of trouble, or from load unbalance.

"Disturbance" is a fault as defined above, a reclosure on or recurrence of a previously established fault. In many instances a single oscillogram showed more than one disturbance due either to the fault re-establishing after being self-cleared or to reclosure of some protective equipment on sustained faults.

Extent of Data

The principal data presented in this paper concerning feeders equipped with instantaneous ground relays and for immediate breaker reclosure were obtained from feeders of four power companies. The extent of these observations is summarized in Table I. A total of 1,498 oscillograms, representing 1,279 separate faults and 1,758 disturbances, was obtained. The various feeders were under observation for periods ranging from about 4 to 48 months and had a total mileage of approximately 450 miles. Some data regarding tree-leakage currents and the performance of repeater-type fuses and pole-top reclosures were taken from observations on feeders equipped with inverse-time phase relays only or with inverse-time phase and ground relays. Details of these feeders are not given herein.

Table II gives the relay settings and reclosure practice applying to each feeder during the observations. The instantaneous ground relays locked out prior to first reclosure of the breaker on the feeders of companies *A*, *B*, and *C* but controlled the breaker for the first two openings in the case of company *D*. Following lockout of the instantaneous relays the feeders were protected by inverse-time phase and in some cases, ground relays.

Performance of Relays and Circuit Breakers

A. Manner of Fault Clearance and Automatic-Reclosure Performance

The manner of clearance of all faults, as deduced from the appearance of the oscillographic records in conjunction with the correlation data supplied by the power companies, is summarized in part *A*, Table III. The percentage of faults cleared by circuit-breaker operation ranged from 13 to 76 per cent. This wide spread in percentage is largely attributable to differences in fusing practice and to use of open-gap lightning protection on transformers of some feeders. The remainder of the faults in each case were either self-clearing or were cleared by other protection, such as transformer and branch-line fuses or pole-top reclosers located out on the feeder. No distinction has been made in tables and charts between these manners of fault clearance since the data were not sufficient, in most instances, for making such a classification. On one feeder (not otherwise considered in this paper) the observing equipment was arranged so that operations of pole-top reclosers, which protect most of the branches, could be identified. It was definitely established that they cleared 27 per cent of the faults. Undoubtedly other faults were also cleared in this manner but the data were not conclusive. The main breaker, which is controlled by inverse-time relays, cleared only about 22 per cent of the faults.

Part *B* of Table III was prepared to show the results of instantaneous relaying and immediate automatic reclosure. Under section *B*1 all initial breaker operations are considered, irrespective of whether the fault current persisted until the breaker opened; that is, included in this category are cases in which a fault initiated a breaker operation, but the current was interrupted by a fuse or otherwise before the breaker started to open. In section *B*2 of the table only those cases are included in which the fault current persisted until the breaker first opened, the breaker being responsible for initial interruption of the fault. This latter category would appear to be the better indication of the benefit that can be attributed directly to instantaneous relaying and rapid reclosure. The data indicate that from 50 to 100 per cent of faults falling in this category were clear on first reclosure, the average for all feeders being about 70 per cent.

There are, of course, a number of factors which affect the manner in which faults are cleared. One of these factors is

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Table I. Data Regarding Power Feeders and Observations

(Refer to Table II for Relaying Details)

Power Feeder*	Feeder Mileages: No. of Phases on Line: 1	2	3	Total	Oscillograph Trip Current (Amp)	Oscillographic Observations**: Period of Observation: Period	Mos.	Extent of Records Obtained§: Oscillograms	Disturbances	Faults	Tree Conditions	Remarks
Company A												Lightning arresters are used with all distribution transformers. On new installations they are connected inside transformer fuses while on some old installations they are connected outside transformer fuses. The four kilovolt feeders are primarily urban residential lighting circuits; feeder *K*-1202 supplies chiefly rural residential load, while feeder *E*-1215 supplies chiefly rural and suburban residential load. There are a few industrial loads taken from both feeders.
L-402 (4 kv)	10.9	0	6.6	17.5	50	6-14-34 to 1-1-36	19	31	31	30	Bad	
L-406 (4 kv)	4.7	0	10.3	15.0	50			13	13	13	Good	
K-1202 (12 kv)	13.3	7.7	4.3	25.3	30	10-25-33 to 8-15-34 2-18-37 to 5-12-38	25	36	44	36	Good	
E-1215 (12 kv)	72.0	0	25.0	97.0	75	6-8-34 to 5-12-38	48	164	187	157	Fair	
Company B—6.9 kv												Lightning arresters are used with all transformers. They are connected outside the fuses and have separate driven grounds. These feeders serve urban territory.
19	3.9	0	6.6	10.5	35	11-27-34 to 10-13-38	47	40	50	24	Average	
20	14.0	0	23.0	37.0	35	10-23-34 to 10-13-38	48	150	201	69	Average	
Company C—12 kv												
3003	90.9	0	57.7	148.6	45	7-2-37 to 3-7-39	20	683	785	633	Average	In areas where fuses and relays can be co-ordinated, transformers are equipped only with spill gaps (two gaps in series) for lightning protection. Where fuses and relays cannot be co-ordinated, lightning arresters are used. Fuses are placed on the transformer side of the arresters; where gaps are separate units the fuse is on the line side of the gap. This feeder serves chiefly residential load in suburban and rural areas.
Company D—13.8 kv												
7*M*35	4.0	4.0	6.0	14.0	40	3-9-38 to 7-11-38	4	26	26	22	Light	Plain spill gaps or expulsion type spill gaps are used for lightning protection of transformers. This feeder serves urban territory and the load is about 75 per cent residential and 25 per cent commercial.
Halls	71.0	10.0	0	81.0	20	5-26-36 to 7-11-38	25	355	421	295	Light	No lightning arresters are used but gaps, connected outside the fuse, are used with every transformer. This feeder supplies rural territory which has a customer density of about seven per mile.
Totals	284.7	21.7	139.5	445.9				1,498	1,758	1,279		

* All feeders are equipped with an instantaneous ground relay and for immediate reclosure of the breaker. Feeders of companies *A*, *C*, and *D* are three-phase, four-wire, multigrounded neutral feeders, while those of company *B* are three-phase, three-wire with the neutral grounded through a grounding transformer bank.

** Power system residual current was recorded in each instance.

§ These records are from accidental disturbances and do not include records from unbalanced load currents and staged tests. Where the oscillograph repeated on a sustained fault the series of records obtained has been counted as one instance.

the nature of the fault itself, that is, it may be of very short duration, probably due to a momentary flashover which is self-clearing before a breaker or fuse has time to operate or it may be cleared by the operation of small transformer fuses. Where instantaneous ground relays are used it is intended that they operate to clear such faults before other protective equipment, excepting small fuses, can operate.

In the case of feeder *E*-1215 of company *A*, a cold cathode tube relay was used as the instantaneous ground relay for a portion of the observation period. At first it was used with no purposely introduced time delay; later, time delay of two cycles was introduced to prevent unnecessary tripping. Without time delay its operating time was approximately 0.007 second. As it has no inverse-time characteristic, operation is initiated practically as soon as the operating current is reached. This is an advantage if the fault is going to persist. However, if the fault consists solely of very short lived kicks, it is desirable to have a small delay in relay operation. A comparison of the operation of the tube-type relay with and without time delay of two cycles is given in Table *A*. From these data it is evident that slowing

Table II. Type of Relaying and Reclosure Practice

	Phase Relays				Ground Relays					
					Inverse Time				Instantaneous	
Power Feeder	C.T. Ratio	Pri. Pickup Current (Amp)	Time Lever Setting	Current for 1 Sec. Operation (Amp)*	C.T. Ratio	Pri. Pickup Current (Amp)	Time Lever Setting	Current for 1 Sec. Operation (Amp)*	Pri. Pickup Current (Amp)	Reclosure Practice
Company A										
L-402 and *L*-406 (4 kv)	40/1	320	3	650	None				80##	1 (immediate) automatic**
K-1202 } 12 kv	40/1	160	3	640	None				80	3 automatic—first is immediate**
E-1215 } 12 kv	60/1	300	3	1,200	None				150§§	1 (immediate) automatic**
Company B—6.9 kv										
19	60/1	360	2 1/2	650	60/1	90	5	900#	60	1 (immediate) automatic**
20	60/1	360	2 1/2	650	60/1	80	5	800#	60	
Company C—12 kv										
3003	40/1	320	2	800	None				40	1 (immediate) automatic**
Company D—13.8 kv										
7*M*35	40/1	400 400§	2 1/2	800	40/1	40	2 1/2	80	80	Before 5-24-38 3 automatic: immediate, 30 seconds and 105 seconds***
	40/1	320 400§	1	400	40/1	40	6 1/2	200#	100	After 5-24-38
Halls	5/1	60 60§	7	260#	25/1	50	6	160#	25	3 automatic: immediate, 45 seconds and 120 seconds***

*Refers to relay operation only; does not include breaker time.

**Instantaneous ground relay locks out following initial breaker operation leaving inverse-time relays in control until recloser is manually reset.

***Instantaneous ground relay has control until after second trip-out.

§Instantaneous phase relays.

§§Solenoid type relay from 6-8-34 to 4-15-35. Gas tube relay without time delay from 4-15-35 to 10-10-35 and from 3-15-37 to 5-12-38. Gas tube relay with 2 cycles delay from 10-10-35 to 3-15-37.

#Current required for 1 1/2 second operation.

##Experimental only.

Table III. Manner of Fault Clearance and Automatic Reclosure Performance

	Company A								Company B				Company C		Company D			
	4 Kv				12 Kv				6.9 Kv				12 Kv		13.8 Kv			
	L-402		L-406		K-1202		E-1215		19		20		3003		7M35		Halls	
	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent
Total faults	30	100	13	100	36	100	157	100	24	100	69	100	633	100	21	100	295	100
A. Manner of fault clearance																		
a. Breaker-cleared	4	13	4	31	9	25	29	19	4	17	40	58	375	59	10	48	225	76
b. Self-or fuse-cleared	26	87	9	69	27	75	128	81	20	83	29	42	258	41	11	52	70	24
B. Automatic reclosure performance																		
(1) Considering all initial breaker operations																		
a. Total	15	100	5	100	18	100	110	100	7	100	65	100	570	100	17	100	274	100
b. Clear on first reclosure	15	100	4	80	14	78	91	83	5	71	42	65	464	81	13	76	221	81
c. Clear following first reclosure#			4	80	17	94	107	97	7	100	50	77	568	100	13	76	224	82
d. Clear on second reclosure##			4	80	18	100	109	99			53	82	569	100	16	94	243	89
(2) Considering only cases where fault current persisted until breaker first opened																		
a. Total	4	100	4	100	12	100	42	100	6	100	48	100	479	100	10	100	254	100
b. Clear on first reclosure#	4	100	3	75	8	67	26	62	4	67	25	52	373	78	6	60	202	80
c. Clear following first reclosure#			3	75	11	92	42	100	6	100	33	69	477	100	6	60	205	81
d. Clear on second reclosure##			3	75	12	100	42	100			36	75	478	100	9	90	224	88

#Faults which did not involve a second breaker opening. This item is the sum of (*b*) plus those faults which were fuse or self-cleared following first breaker reclosure.

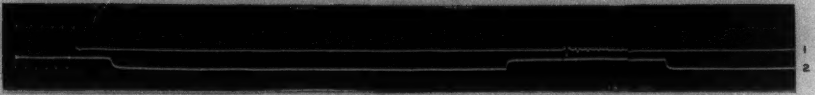
##Sum of faults in (*c*) above plus those cleared on second breaker reclosure. Second reclosure was not immediate except for company *D*.

down the relay saved a number of unnecessary trippings on very short duration disturbances.

B. SPEED OF CLEARANCE AND RECLOSURE

The recorded average time from initiation of fault to first breaker opening ranged from 7 to 15 cycles for the various feeders; the average time from initiation of fault to first immediate reclosure ranged from 34 to 57 cycles. In arriving at these times, one cycle has been added to the duration shown by the oscillograms to allow for the time it takes the oscillograph film to get in motion.

The oscillograms pictured in Figure 1 (A) and (B) illustrate faults which were breaker-cleared by operation of instantaneous ground relays which control the breaker for two openings. (Element 2, which indicates breaker operation, was controlled from a contact on the breaker whose opening lagged the actual breaker

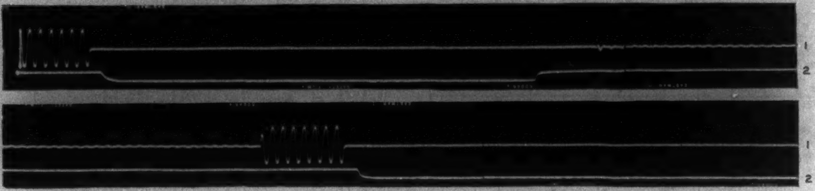


(A)

ILLUSTRATES:- OPERATION OF INSTANTANEOUS GROUND RELAY AND IMMEDIATE RECLOSURE; RECLOSURE TRANSIENT; FALSE BREAKER OPERATION FROM RECLOSURE TRANSIENT.

EL. 1-RESIDUAL CURRENT

EL. 2-INDICATES BREAKER OPENINGS AND RECLOSURES

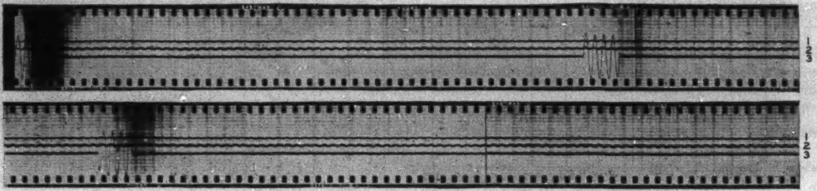


(B)

ILLUSTRATES:-TWO OPERATIONS IN SEQUENCE OF INSTANTANEOUS GROUND RELAY AND FIRST IMMEDIATE RECLOSURE.

EL. 1-RESIDUAL CURRENT

EL. 2-INDICATES BREAKER OPENINGS AND RECLOSURES

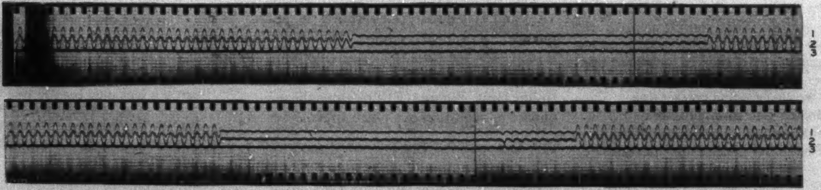


(C)

ILLUSTRATES:-THREE OPERATIONS IN SEQUENCE OF REPEATER-TYPE FUSES; ONE LINE-TO-GROUND FAULT.

EL. 1 AND 2-PHASE C AND B CURRENTS

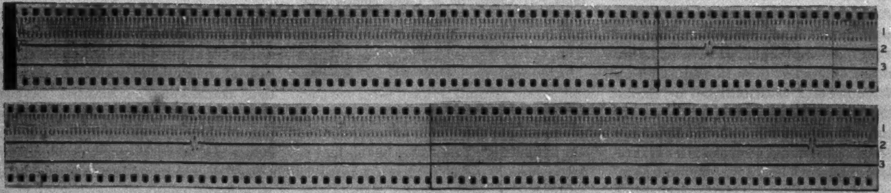
EL. 3-RESIDUAL CURRENT



(D)

ILLUSTRATES:-THREE OPERATIONS IN SEQUENCE OF REPEATER-TYPE FUSES; LINE-TO-LINE FAULT.

EL. 1, 2 AND 3-PHASE A, B AND C CURRENTS



(E)

ILLUSTRATES:-OPERATIONS IN SEQUENCE OF AN FP-19 POLE-TOP OIL CIRCUIT-RECLOSER

EL. 1-INDICATES WHEN SUBSTATION BREAKER OPENS OR CLOSES-FEEDER VOLTAGE AT SUBSTATION

EL. 2-NEUTRAL CURRENT

EL. 3-NOT USED

Figure 1. Illustrative oscillograms

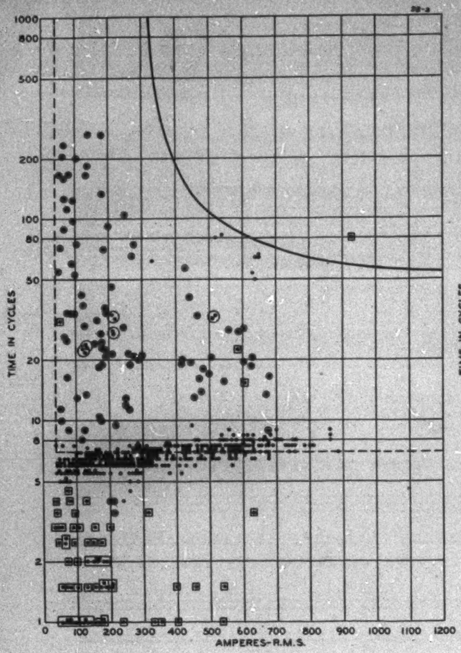


Figure 2. Magnitude and duration of fault currents

Company C-12 kv Feeder 3003
7-2-37 to 3-7-39

——— Approximate phase-relay characteristic—seven cycles allowed for breaker time
— — — Instantaneous ground-relay characteristic including breaker time
● Breaker-cleared disturbances
⊡ Fuse- or self-cleared disturbances
⊙ Fuse- or self-cleared after first reclosure

openings and consequently closed slightly ahead of the breaker.) The first illustration also shows a typical transient following breaker reclosure and an instance in which the transient caused a false relay operation. These transients are discussed in the following section. The second illustration shows two operations in sequence of the breaker, both initiated by the instantaneous relay. In this latter case the fault was temporarily cleared but became reestablished about 40 cycles after first reclosure took place.

Observations on several feeders which were equipped near the substation with repeater-type fuses (three-shot), indicate an average reclosure time of the fuses of 32 cycles. Other observations indicate that pole-top reclosures, of the type used, operate in from two to four cycles and reclose in from two to four seconds.

The oscillograms pictured in Figure 1(C) and (D) illustrate operation in sequence of repeater-type fuses. The first illustration is for a one line-to-ground fault while the second illustration is for a line-to-line fault. Figure 1(E) illustrates the sequence of operation of a pole-top recloser.

C. Transients Following Breaker Reclosure

Reclosure of breakers on sound feeders usually produced residual current transients of fundamental frequency and nonsymmetrical wave form caused by the inrush of magnetizing current to distribution transformers connected between phase wires and neutral. The general nature of these transients is illustrated in Figure 1(A) which shows an oscillogram of a fault cleared by an instantaneous relay on a feeder equipped for immediate reclosure. In this instance the transient operated the instantaneous relay and retripped the breaker.

These transients have been studied with regard to their possible limitation on re-

Table A. Operation of Cold-Cathode-Tube Relay

	Without Delay* (20 Months)	With Delay (17 Months)
Total breaker openings considered	60	26
Number showing fault current duration of less than two cycles	34 (57%)	9 (35%)
Average time (cycles)—initiation to breaker opening	7.0†	8.8†
Average time (cycles)—initiation to reclosure	34.3†	35.0†

* Operation in 0.007 second.

† One cycle added to allow for time required for oscillograph film to get in motion.

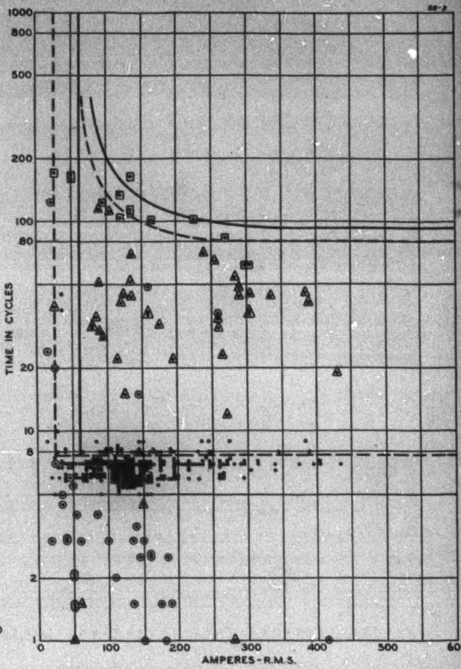


Figure 3. Magnitude and duration of fault currents

Company *D*-13.8 kv Halls feeder
5-26-36 to 7-11-38

——— Approximate phase-relay characteristic—eight cycles allowed for breaker time
— — — Approximate ground-relay characteristics including breaker time

●	Breaker-cleared disturbances	Instantaneous relays in service
⊙	Fuse- or self-cleared disturbances	
⊡	Breaker-cleared disturbances	Instantaneous relays locked out
△	Fuse- or self-cleared disturbances	

lay pickup current settings. It may be said, in general, that where instantaneous relays have control until after first reclosure (Halls feeder and feeder 7*M*35 utilize this condition) such transients may place a minimum limit on relay pickup current settings. However, this limit need not be the maximum value of the reclosure transient (except perhaps in the case of tube relays which have no purposely introduced time delay) because the maximum magnitude of the transient occurs only within the first half cycle, the transient attentuates rapidly and, due to the almost complete suppression of the upper (or lower) half of the wave, the effective value is much less than that of a sine wave of equivalent peak value. This is well illustrated by the fact that only eight cases of false relay operation were recorded on the Halls feeder from reclosure transients although the instantaneous ground relay was set to pick up on 25 amperes and the recorded maximum currents from reclosure transients ranged up to 125 amperes. The currents from this cause recorded on the other systems ranged up to 300 amperes but no relay operations could be definitely attributed to reclosure transients. Current values quoted for reclosure transients are peak amperes divided by $\sqrt{2}$.

D. Unbalanced Load Currents

A study of the oscillographic data was made to determine whether unbalanced load currents in four-wire, multigrounded neutral feeders might impose an important limitation on the pickup current settings of ground relays in the range from 50 to 100 amperes. The data obtained are summarized in Table *B*.

Table B. Data Regarding Unbalanced Local Currents

Feeder	Approx. Oscillograph Trip Amps.	Instantaneous Ground Relay Pickup Amps.	Oscillograms Due to Unbalanced Load Currents: Total No.*	Above Current of: Amps	Above Current of: No.	Current Magnitude Range Amps.**	Max. Normal Load Phase Current Amps.
Company A—4 kv							
L-402	50	80	16	80	1	40–110	150–200
L-406	60	80	17	80	6	45–140	150–200
Company A—12 kv							
K-1202	30	80	112	40	0	20	10–20 (1934)
E-1215	75	150	4	75	2	25–90	100–150 (1934)
Company C—12 kv							
3003	45	40	Few			40	60 (1935)
Company D—13.8 kv†							
Hall's	20	25	7			5	2 (1935)

*In this tabulation a series of oscillograms obtained while the oscillograph was continuously operating due to the same unbalance is counted as one oscillogram.

**Sustained current magnitudes. In a few instances the current magnitudes momentarily exceeded these values.

†No records of measurable magnitude were obtained from Feeder 7*M*35.

Unbalanced currents due to switching and temporary two-phase operation, as well as those due to normal load unbalance, have been included in this tabulation. The figures under the column headed "Total No." represent separate instances in which the unbalanced currents reached values sufficient to produce oscillograms. Momentary load surges sometimes tripped the oscillograph as is evinced by the fact that sustained currents lower than the oscillograph tripping currents were recorded. Only in the case of the four-kilovolt feeders of company *A* did the unbalanced load current exceed the relay pickup current, and the only instance of breaker operation due to unbalanced load current occurred on one of these feeders. The ground relays on these feeders were an experimental installation, and it seems to have been the practice to cut out these relays when, during emergency operation, a feeder was unbalanced by load being switched to or from another feeder. Most of the large unbalanced currents were recorded during such emergency operation.

These data are limited but indicate that ground relay settings of from 50 to 100 per cent of normal maximum load current but not less than 25 to 35 amperes would be realizable.

Effectiveness of Circuit Breakers and Fuses in Clearing Faults

In order to study the efficacy of relays, circuit breakers and fuses in clearing faulted lines, "shotgun" diagrams showing the magnitude and duration of all observed ground-fault currents having durations of one cycle or over were prepared for each feeder. On each of the diagrams the approximate relaying time-current characteristic curves also were plotted. Illustrations of this type of diagram are given in Figures 2 and 3 for feeder 3003 and the Hall's feeder respectively. In these figures the lines and curves coded as relay characteristics include relay plus breaker time. For instantaneous relays the time of operation, considered the same for all currents above pickup current, was taken as the average duration of instantaneously relayed faults, as determined from oscillographic records, plus one cycle to allow time for the oscillograph to get in operation. The plotted points represent the durations of the individual disturbances, as shown by the oscillograms, without any allowance for oscillograph starting time, a procedure followed to simplify handling the data. This, together with the fact that it is not always possible to determine accurately the duration from every record, accounts to some extent for the distribution of points above and below the instantaneous relay characteristic.

In general, the maximum current magnitude shown by each oscillographic record was used in plotting the points in the figures. Since most of the oscillograms showed a fairly uniform magnitude wave trace, the relays and fuses were usually subjected throughout the duration of the disturbance to currents of approximately the values plotted. A code is used to indicate the manner of clearance of the disturbances.

The results of these analyses, which were made for all feeders, indicate that the manner of fault clearance was consistent with what would be expected from the relay and fuse characteristic curves and other known circumstances at the times of the disturbances.

Performance of Fuses

In every case which could be used for this phase of the study, the fuse was found to have cleared within the time shown by its characteristic curve. In many instances where the clearing was apparently much faster than expected, the correlation data indicated that more than one phase conductor was involved in the fault. Some cases of damage to fuse holders were reported but there is no indication that the arc was sustained after the fuse blew.

Effectiveness of Fuse and Relay Co-ordination

Where high-speed (instantaneous) relaying and rapid reclosure are employed, the type of co-ordination sought is to protect, in so far as possible, feeder and

Table IV. Faults Classified as to Primary Cause and Nature of Trouble*

	Company A				Company B		Company C		Company D			
	4 Kv		12 Kv		6.9 Kv		12 Kv		13.8 Kv			
	L-402 and L-406		K-1202 and E-1215		19 and 20§		3003		7M35		Halls	
Description	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent
A. Classification as to primary cause												
1. Lightning or storm during lightning season (April 15–Sept. 15)	24	56	108	56	46	39	10	1	2	9	81	28
2. Primary cause unknown—occurred during lightning season	8	18	26	13	11	9	372	59	9	41	115	39
Sum of above—probably due to lightning in most instances	32	74	134	69	57	48	382	60	11	50	196	67
3. Wind, rain, sleet, and/or snow	3	7	11	6	34	29	1	0.1	3	14	25	8
4. Foreign objects in line (other than caused by 3 above)	2	4	20	10	9	8	6	0.9	8	36	17	6
5. Primary cause unknown—occurred outside lightning season (Sept. 15–April 15)	6	15	28	15	18	15	244	39	0	0	57	19
Total faults	43		193		118		633		22		295	
B. Classification as to nature of trouble												
1. Flashovers	37	87	146	76	62	52	610	96.3	10	45	260	88
2. Insulator or bushing failure	1	2	0		0		0		0		1	0.3
3. Wires down	1	2	15	8	41	35	2	0.3	3	14	15	5
4. Birds or animals in line	0		1	0.5	0		3	0.5	8	36	0	
5. Poles broken or blown over	1	2	2	1	2	2	0		0		0	
6. Whipping conductors	0		0		5	4	1	0.2	0		0	
7. Crossed wires	0		7	3.5	0		1	0.2	0		0	
8. Equipment failures	0		17	9	7	6	16	2.5	0		12	4
9. Trees or limbs in line (exclusive of those causing broken or crossed conductors)	3	7	5	2	1	1	0		1	5	7	2.7

*Correlation data were most complete in the case of companies *A* and *B* and feeder 7*M*35 of company *D*.

§These data include some faults not recorded by the oscillograph but which were indicated by the correlation data.

Explanation of items used in classification:

Flashovers—only cases where no damage other than fuse operation was reported. While a flashover is not definitely known to have occurred, the presumption from the character of the disturbance, is that such was the case. Many faults of short duration fall in this category.

Conductors down—phase conductors, ground wires, etc.—no structures reported down.

Equipment failure—transformers, lightning arresters, etc.

Whipping conductors—contacts with other conductors, line structures, or structures adjacent to line.

Foreign objects—wires, trees, or limbs in line, automobiles striking poles, etc.

branch-line fuses during the first, and in some cases the second, tripout, so as to give faults, such as arc-overs due to lightning, an opportunity to clear before branch circuits or sections of the main feeder are interrupted and before permanent damage occurs. To provide against the contingency of permanent faults, the instantaneous relay is usually cut out prior to or in some cases following the first reclosure, and inverse-time relays are then depended upon for back-up protection of the circuit. The inverse-time relay in this case is set to allow branch-line fuses to clear before the breaker again operates. Thus, the objective is to prevent fuses from blowing by the use of instantaneous relays, while allowing them to blow before the inverse-time relay operates.

In the case of the 12-kv feeders of company *A* and the feeders of company *B*, branch-line fuses were co-ordinated with inverse-time relays and some benefit in the protection of these fuses apparently resulted from the use of instantaneous relays. However, with the speed of breaker operation utilized on feeders of the capacity of these it appears impracticable to protect branch-line fuses of about 40 amperes capacity or less, as these smaller rated fuses usually blew before the breaker, operated by an instantaneous relay, could clear the fault. Co-ordination between fuses and inverse-time relays is evidenced by the fact that the inverse-time relays were seldom called upon to clear a disturbance.

In the case of company *C*, it may be said that co-ordination appears to be good, since so many faults were cleared by the instantaneous relay and since so few of those which persisted beyond first reclosure were breaker-cleared.

The Halls feeder has relatively few fuses, the maximum size being 40 amperes. Since the feeder is fused near the substation, a fault at almost any location will have a fuse between it and the substation. Only 27 branch-fuse operations have been correlated with records of disturbances on this feeder and in each case they operated following lockout of the instantaneous relay but before the inverse-time relay contacts could close. Operations of the inverse-time relays occurred on other faults following lockout of instantaneous relays. In all but four cases the current magnitude, with respect to the duration shown on the oscillogram, was below that for which a 40-ampere fuse would co-ordinate with the inverse-time relays. In three of these four cases there was apparently no fuse between the fault and substation; in the other case the circumstances are not known. Thus, benefit apparently accrued from the protection of branch-line fuses by instantaneous relays. Also, the use of instantaneous relays may have prevented some cases of wire failure.

The data from the four-kilovolt feeders of company *A* and feeder 7*M*35 of company *D* are too meager to give much indication on this subject. However, it is probable that some benefit accrued from the protection of branch fuses by instantaneous relays.

Effect of Ground Relaying on Continuity of Power Supply

This study indicates that as compared with feeders utilizing inverse-time relays only, the use of instantaneous ground relays, while causing more frequent breaker operations, has increased the continuity of power supply, in that some branch-fuse

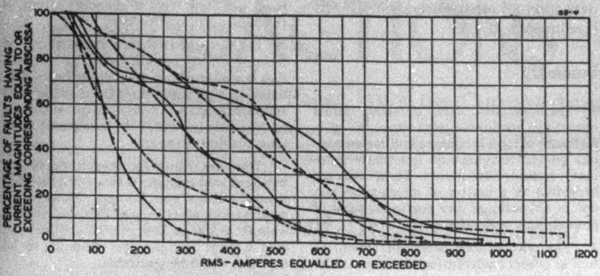


Figure 4. Magnitude Distribution of Recorded Sustained* Residual Currents

Code	Feeder	Feeder Kv	Approx. Max Fault Current at Substation Bus—RMS Amperes⊕	Highest Current Recorded —RMS Amperes*
——————	L-402 & L-406	4	4,500	1,020
— — — —	19	6.9	3,000	1,140
●——●	20	6.9	3,000	960
●— — —●	K-1202	12	5,000	850
——————	E-1215	12	2,400	680
○———○	3003	12	4,000	1,030
●——— —●	Halls	13.8	425	420†

† Data from feeder 7M35 were not sufficient for preparing a curve.

* Largest magnitude sustained for three cycles or more.

⊕ For one line-to-ground fault.

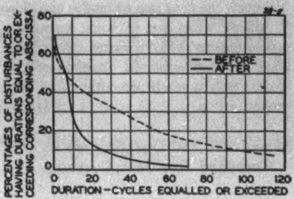


Figure 5. Distribution of disturbance durations

Data obtained from feeder K-1202 before and after instantaneous ground relay installed

Table V. Classification of Faults Involving Broken or Burned-Down Wires

Primary Cause	Company A 4-Kv L-406	Company A 12-Kv K-1202 and E-1215	Company B 6.9-Kv§ 19 and 20	Company C 12-Kv 3003	Company D 13.8-Kv 7M35	Company D 13.8-Kv Halls
1. Lightning	0	1	13	0	1	1
2. Wind	1	6	19	0	2	8
3. Acts of man	0	4	7	2	0	5
4. Trees or limbs—other than those cut or blown into line	0	4	0	0	0	1
Total faults	1 (7.7)*	15 (7.8)*	39 (42.0)*	2 (0.3)*	3 (13.6)*	15 (5.1)*

*Figures in parentheses give faults involving broken or burned-down wires in per cent of total faults recorded.

§25-Cycle feeders are carried on the same poles with feeder 20 at certain locations. Eight of the faults involving broken wires could be associated with contact or flashovers between the wires of these two feeders.

operations were prevented and probably some wire failures as well. Also, the instantaneous relays clear the fault much more quickly than do inverse-time relays. Thus, in the case of faults which were not permanent, momentary outages on the feeders were substituted for long time outages. Where instantaneous relays are set to pick up at much lower current than the inverse-time phase relays (see Figure 2) they furnish added assurance of positive disconnection in the fault current range between the two relay pickup current values, 40 to 320 amperes in this case. It should be pointed out that, except on feeders having limited fault current, instantaneous ground relays would not be expected to save branch-line fuses below 30 or 40-ampere rating.

Cause and Nature of Faults

As far as it has been practicable, all oscillographic records have been correlated with cause and nature of the trouble on the power feeders. In Table IV all faults are classified in accordance with the attributed primary cause and nature of the trouble. The lightning classification has been divided into two parts; one includes faults definitely correlated as due to lightning or to a storm during the lightning season; the other includes correlated plus uncorrelated faults whose causes and natures were unknown but which occurred during the lightning season (April 15–September 15). Experience indicates that these latter disturbances are, in most instances, due to electrical storms. The sum of these two is believed to represent more nearly the true effect of lightning. The data show that lightning has been the major cause of trouble, accounting in general for about 60 per cent of the total faults.

Trees have been reported as being responsible for only a small percentage of the faults and the trouble from them was largely due to limbs or trees falling into, or being blown or cut into the lines. The data from none of the systems gave evidence of intermittent or sustained leakage currents which might be a possible limitation to ground relaying. This has been substantiated by staged tests, conducted by two of the co-operating power companies, utilizing bare copper wires on 4 and 11-kv power circuits. The tests were made when the sap was in the trees and contact with the energized conductor was much better than would ordinarily occur with a wire in accidental contact with a tree limb. The maximum fault current of 10.5 amperes was recorded for a test in which the conductor was connected to a spike driven into a live willow tree trunk two feet above ground. The next highest current (nine amperes) was obtained from a test in which the conductor was wrapped several times around a dogwood tree trunk seven feet from the ground and pulled tight enough to break through the outer bark.

The above indicates that one or a few tree grounds are not likely to draw enough current to cause ground relay operation except where pickup currents are quite low, say below 25 amperes. It, therefore, appears likely that most tree trouble is due to branches getting across phase conductors, between phase conductors and neutral, or pushing the wires together, rather than to leakage through the trees to ground.

Faults Involving Broken and Burned-Down Wires*

All correlation data from each power system have been examined to determine, as far as practicable, those faults which apparently involved broken wires. The results as regards the causes of these faults are summarized in Table V. The major causes were wind and acts of man.

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* In this section no distinction is made between broken and burned-down wires or between phase conductors and ground wires.

Although the data regarding the question of broken wires are, in some instances, rather meager, they indicate that of the total faults experienced on most of the feeders only a small percentage involved broken wires.

Magnitude and Duration of Recorded Residual Currents

Data obtained regarding the magnitude distribution of sustained fault currents (largest magnitude sustained for three cycles or more) are given by cumulative percentage curves in Figure 4. The ordinates of these curves indicate the percentage of faults in which the current magnitudes equalled or exceeded the corresponding abscissa. Since, in most instances, the wave traces are fairly uniform, the distribution of maximum current values would be essentially the same as is shown in Figure 4.

The duration distribution of the currents recorded on feeder *K*-1202 of company *A* are given by cumulative percentage curves in Figure 5. One curve is for the records obtained after the feeder was equipped with an instantaneous ground relay; the other curve is for the period when the feeder was equipped only with inverse-time relays and is included to show the effect of instantaneous relaying. The first mentioned curve is typical of the data from all of the instantaneously relayed feeders.

Summary

1. Oscillographic data obtained under operating conditions regarding the performance of feeders equipped for instantaneous ground relaying and immediate automatic reclosure are given. These data are presented as a matter of information and not in advocacy of any particular form of ground-fault protection.

2. The observations described herein, as well as others made during the same study, indicate that relays, fuses, and circuit interrupters operate consistently and reliably on currents at or above which they are set or designed to operate.

3. Considering only faults which persisted at least until initial breaker-opening, the percentage in which feeders were clear on first (immediate) reclosure ranged from 50 to 100 per cent, the average being about 70 per cent.

4. The average time from initiation of fault to first breaker-opening ranged from 7 to 15 cycles for the various feeders; average time from initiation of fault to first reclosure ranged from 34 to 57 cycles.

5. A main objective of instantaneous relaying and immediate reclosure is to substitute a brief outage on the whole feeder for a long time outage on a branch by preventing branch-line fuse outages on temporary faults such as flashovers. This was successfully accomplished in most instances for the feeders under observation except where fuses were less than 40 amperes rating on the higher capacity feeders.

6. On four-wire feeders, a transient caused by magnetizing current taken by distribution transformers on breaker reclosure may limit the pickup current for which instantaneous ground relays can be set if these relays retain control of the breaker for more than the first opening.

7. Data regarding the magnitude of unbalanced load current on the multigrounded neutral circuits were limited. However, it would appear that ground relay settings of from 50 to 100 per cent of normal maximum load current but not less than 25 to 35 amperes would be realizable.

8. Lightning was the major cause of trouble, accounting in general for about 60 per cent of the total faults.

9. Trees were responsible for only a small proportion of the total faults. Trouble from them was largely due to branches or trees falling into or being cut or blown into the lines. On no system was there evidence of intermittent or sustained tree leakage being a possible limitation to ground relaying with pickup currents of the order indicated in (7) above. This is further substantiated by staged tests in which the current drawn by well made tree grounds was measured.

Reference

1. Protection Features for the Joint Use of Wood Poles Carrying Communication Circuits and Power-Distribution Circuits, Above 5,000 Volts, J. O'R. Coleman and A. H. Schirmer. AIEE Transactions. volume 57, 1938 (March section), pages 131–40.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1942 Supplement to Electrical Engineering—Transactions Section

High-Capacity Circuit-Breaker Testing Station

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TEN years' experience with a high-power laboratory, giving approximately 1,100,000 three-phase initial symmetrical kilovolt-amperes on short circuit at the machine bus,[1] has defined its usefulness and limitations and has resulted in an addition to its installed equipment. By taking advantage of assymmetry this capacity has been sufficient to demonstrate ratings up to 1,500,000 kva, and the increased testing capacity is required to demonstrate interrupting ratings of 2,500,000 kva.

The previous equipment was capable of testing most circuit breakers up to their interrupting rating, but for the largest sizes special methods of testing had to be used to approximate the conditions at the breaker rating.

1. Single-phase testing on three-phase devices. This method is generally satisfactory providing the influence of adjacent phases on closing operation, speed of contact action, and restored voltage conditions are allowed for. It is particularly valuable on large designs with good phase isolation, but must be carefully supervised on compact designs where the influence of adjacent phases may be great.

2. Double-phase to ground on a single-pole,[2] with the contact cross bar grounded, was first used in Europe and is valuable on multiple-interrupter devices as it increases the total voltage on the pole unit for a given current and gives tank pressures corresponding to energy losses not otherwise obtainable. However, its use requires care, as the first interrupter to clear opens only 87 per cent of phase-to-phase voltage, and the voltage across the second interrupter is limited to the phase-to-phase value. Thus, a double 66-kv connection used to demonstrate 132-kv is limited in that the first interrupter opens 57.4-kv and the second opens 66-kv.

3. The use of high superposed voltage on a low-voltage circuit[3] has been advocated and discussed widely. Our experience with it indicates it is not sufficiently reliable for general use since the current wave form of the low-voltage circuit as it approaches final zero is affected, and leakage currents in the arc space around current zero are altered.

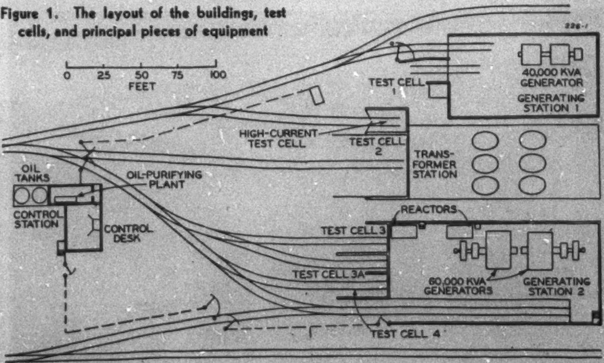
Because of these limitations in testing capacity, it has been desirable in some cases to design circuit breakers with multiple interrupters arranged for adequate distribution of voltage between them, each of which either singly or in groups can be adequately demonstrated for the maximum duty imposed on it. Eight and ten units per pole have been designed for very severe apparatus conditions and these designs have been presented to the Institute.[4] This practice, while giving satisfactory results in operation, requires a complication of design which should not be necessary in the future with the increased demand for high-power and high-speed switching.

On the other hand, when an attempt is made to simplify the structure of high-voltage and high-power interrupting devices, the need for increased testing capacity is evident, since the burden on the individual interrupter is increased and its reliability must also be above question. As an illustration, at the 1941 winter convention a single interrupter for 2,500,000-kva service at 132 kv was presented.[5] This trend, moreover, will be prominent in future design because of low arc energy, simplification of structure, and reduced cost.

Also, the use of compressed air for powerhouse installations is growing and 2,500,000-kva interrupting-capacity devices have been built and tested using a single break per pole. There are possibilities of space saving with this type, but such space reduction of three-pole devices increases the hazard of adjacent poles, and makes three-phase testing under full power and voltage conditions desirable. The results of such tests made in the laboratory here discussed are given in the paper by Ludwig, Wilcox, and Baker at this convention.

The laboratory described below is felt adequate for the future demands of American switchgear practice; in fact, its complete power is necessary for the largest

Figure 1. The layout of the buildings, test cells, and principal pieces of equipment



Paper 42-26, recommended by the AIEE committee on protective devices, for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 18, 1941; made available for printing December 8, 1941.

J. B. MacNeill is manager of the engineering switchgear division, and W. B. Batten an engineer, both with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. The authors wish to express their thanks to Mr. R. C. Van Sickle for his suggestions and assistance in preparing this paper.

Figure 2. Station 2—a second generator doubles the short-circuit capacity

capacities only. However, it is in these large capacities that the major problems of switchgear design occur, including high-speed interruption, high-speed reclosing, and repetitive duty. These problems require continued activity, not only on existing types of apparatus, but especially on those newer forms now becoming active in this country, and on which a background of field experience at high power is lacking.

Development and Use of the Laboratories

The Westinghouse program of providing equipment for high-capacity short-circuit testing was inaugurated in 1925 when generating equipment capable of producing 400,000 initial three-phase symmetrical short-circuit kilovolt-amperes was installed. This was available at generator voltages only. Extensive use was made of this equipment, but transformers to extend the range of test voltages were needed. A bank of three 33,333-kva transformers together with a suitable high-voltage cell was put in service in 1928 to permit testing up to 230 kv. This transformer bank, which was larger than necessary at the time, was selected as the need for more generating capacity was already apparent. In 1930 the second generating station designed to house two 60,000-kva generators (nominal rating) was constructed, and one generator was installed. During the following ten years, this generator was used in making about 47,000 short-circuit tests. The need for more generating capacity became apparent late in this period and the second 60,000-kva unit was put in service in 1940. At this time a second bank of 33,333-kva transformers was installed to provide testing capacity at the higher voltages commensurate with the increased generating capacity.

To supplement this equipment, a high-current low-voltage transformer bank and a high-current test cell were added in 1940, and in 1941 a low-temperature test room was provided.

Since 1925 approximately 100,000 short-circuit tests (including both large and small generators) have been made in these laboratories. Although the greater part of this work has been for the purpose of determining interrupting ability of circuit breakers, many other types of equipment have been subjected to short-circuit tests. Included are:

1. Porcelain insulators—resistance to power arcs
2. Lightning arresters—power-follow tests
3. Power transformers—short-circuit tests
4. Current-limiting reactors
5. Current transformers
6. Fuses
7. Bus bar structures
8. High-voltage capacitors
9. Potential devices

Generating Equipment

The physical arrangement of the essential parts of the Westinghouse Electric and Manufacturing Company's high-capacity testing plant is shown in Figure 1. In the original station 1 is located the 40,000-kva set which consists of two 20,000-kva generators driven by a 3,300-horsepower wound-rotor motor. Separately driven exciters are provided. Since the larger generators of station 2 were put in service, the 40,000-kva plant is used for smaller apparatus which it can test to full short-circuit rating. Also, for most of the work requiring the high-current low-voltage transformers, the 40,000-kva plant has adequate capacity. The generators in station 1 have an initial reactance of approximately 10 per cent, hence a short-circuit output of 400,000

Figure 3. The new transformers and bus structure permit testing to 345 kv three phase

Figure 4. Inside the cold room with a sleet test in progress

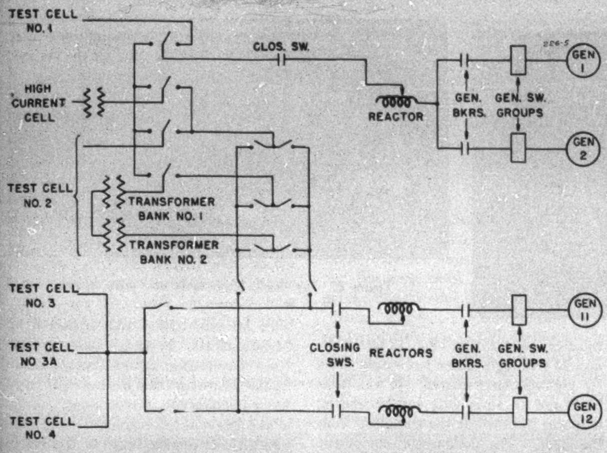


Figure 5. Power circuits combine simplicity and flexibility

initial three-phase symmetrical kilovolt-amperes may be obtained. By different generator coil arrangements full capacity of this plant may be obtained at 3.8, 6.6, 7.6 and 13.2 kv at 60 cycles. No provision is made to operate this plant in parallel with the larger generators in station 2.

In the newer station 2 are located two 60,000-kva (nominal rating) generator sets. These machines when operating in parallel can produce an initial three-phase symmetrical short circuit of approximately 2,000,000 kva at the 13.2-kv test cells. Each of these sets consists of its generator, a 6,000-horsepower wound-rotor driving motor, and a direct-connected exciter. The machines have 14 poles, hence they run at 514 rpm for 60-cycle operation and 219 rpm for 25-cycle operation. Since liquid rheostats are provided for starting, lower frequencies may be obtained by operating with resistance in the rotor circuits.

The two sets are mechanically independent, although provision has been made to couple them if it ever becomes desirable. Because of the 700-kw friction and windage loss of each set, an important saving in power is obtained by operating only one machine when it is adequate.

When parallel operation of the two generators is required, they are synchronized electrically, reactors being provided for this purpose. This operating arrangement has proved very satisfactory. The rotors of the two sets have the same inertia, and under the most severe short-circuit conditions, there is no tendency for the two sets to pull out of step.

From a casual observation (Figure 2) it would appear that the two generators are identical. However, the unit built in 1940 takes advantage of certain refinements in materials and incorporates some of the advances made in generator design. The total weight of each machine is 550 tons, being about equally divided between stator and rotor. The rotor diameter is 12 feet and has a peripheral speed of 22,000 feet per minute. The inertia of each rotor is 8,500,000 pound-feet^2. As is shown in Figure 2, the stators are spring-mounted to cushion the shock on the foundation at the time of short circuit.

Full capacity may be obtained from these generators at 7.6 and 13.2 kv at 60 cycles and at 3.12, 5.5, 6.25 and 11 kv at 25 cycles.

The stored energy in the rotors of these sets is adequate to absorb the losses produced by short circuits, and it is unnecessary to disconnect the driving motors except in special cases where the short circuits are sustained for several seconds. This stored energy presents a problem when bringing the sets to a stop. With no braking, it takes two hours for the sets to come to a standstill. Provision is made to supply d-c to the driving-motor primaries and to let their secondaries feed energy into the liquid rheostats. In this way they can be brought to rest in about eight minutes.

High-Voltage Equipment

To provide for the testing of apparatus at voltages in excess of 13.2 kv, the transformer station is available (Figure 3). This installation includes six 33,333-kva single-phase transformers. They are supplied directly from the generators at 13.2 kv. The secondary of each unit consists of six 22-kv coils. Hence voltages of 22, 38, 44, 66, 76, 88, 115, 132, 152, and 230 kv are available with full capacity. The original three transformers are insulated for 132 kv-to-ground and the newer three for 196 kv-to-ground so that they may be connected in series to give higher voltages. It is, therefore, possible to obtain 345 kv, three-phase *L-L* with the neutral grounded and 396-kv single-phase with mid-point grounded. The 345-kv three-phase connection is made with one winding at 132 kv in series with another at 66 kv. Hence, due to differences in current carrying ability of the transformers, this connection is not suitable for the full capacity of the transformer bank. Various voltages other than those mentioned are

Figure 6. The station operator at the control desk has a direct view of all test cells

Figure 7. Duplicate sequence drums facilitate changes between tests and make possible simultaneous testing at both transformer and generator voltages

Figure 8. The oscillograph table and relay board

obtainable but in most cases not at full capacity as there are unequally loaded transformers involved. The reactance of the transformers is low so that their output, single-phase, is 75 per cent of the generator-bus kilovolt-amperes and on three-phase tests is 65 per cent. Six potential transformers, three insulated for 196 kv-to-ground, are provided for oscillographic recording of voltages.

Power from the transformers is fed to the test cell 2 where all testing at voltages above 13.2 kv is conducted. The only modification required as far as the test cell was concerned, when the second transformer bank was installed, was a new group of roof bushings adequate for 198 kv and a slight increase in the height of the cell to accommodate the larger bushings.

High-Current Transformers

To provide for momentary and five-second current-carrying tests, a bank of low-voltage high-current transformers has been provided. These transformers giving open-circuit voltages of 625, 1,250, 2,500, and 5,000 volts are located in an enclosure which forms a high-current test cell. On the 625-volt connection they are suitable for five-second tests at 200,000 amperes, three-phase or 345,000 amperes single-phase. At these currents the terminal voltage drops less than 50 per cent. These transformers are also useful for interrupting tests at low voltage.

Cold Room

When it was decided to provide a refrigerated room in which high-voltage breakers could be tested under severe sleet and temperature conditions, it became apparent that the high-current test cell could be used for this purpose. This room is about 25 feet long, 12 feet wide, and 25 feet high, hence has ample space for high-voltage breakers. It was insulated and an air-cooling and circulating unit was installed at the end away from the door. The compressor equipment was placed some distance away. Temperatures of −20 degrees Fahrenheit with outside temperature of 90 degrees Fahrenheit are easily obtained (Figure 4). A 110-kv bushing from this cell to the transformer yard provides for high-voltage interrupting tests. This test cell is, therefore, used for fuse testing, high-current testing, and low-temperature work.

Power Circuits

When the second 60,000-kva generator was installed, a number of changes was made in the bus system providing a more direct run to the test cells and the transformers. The arrangement of the power circuits (Figure 5) was made with the idea of flexibility as one of the principal considerations. Each generator has its own breaker, current-limiting reactor, and closing switch. Disconnecting switches are provided so that either or both 60,000-kva generators can supply power to the 13.2-kv test cells, either or both to either transformer bank, and either or both to the high-current low-voltage transformers, or directly to the high-voltage test cell 2. The two 20,000-kva units can supply power to cell 1, to the high-current transformers or the high-voltage cell, and to transformer bank 1.

The connections from the generator coils to the generator setup switches consist of flexible cable firmly cleated in place. From the setup switches to the closing switches, copper tubing covered with Micarta tubing is used. This assembly is supported by Micarta cleats spaced at short intervals. Beyond the closing switches, the bus carrying the output of both generators consists of a square tubular copper conductor similarly insulated with Micarta tubing and supported by Micarta cleats. In the test cells the same type of conductor is used but porcelain insulators are provided instead of Micarta tubes and cleats.

The two banks of current limiting reactors provide a wide range of flexibility. Each bank consists of 18 units (6 per phase). By means of disconnecting switches nearly 100 combinations are available, hence the reactance can be varied from its maximum of 5 ohms per phase to 0 in small steps.

It will be noted from Figure 5 that there is a total of six test cells available, thus making possible the assembling or dismantling of apparatus in some cells while tests are being conducted in others.

Control

The operation of the equipment in station 2 is controlled from a central control desk located in the control house (Figure 6), so arranged that the operator observes the equipment under test. This desk is very complete with indicating instruments, protective relays, annunciators, and buzzer for all protective devices and test jacks for checking all measuring and control circuits.

At various points about the laboratory and yard are located safety switches. Anyone working in the area covered by one of these switches moves the switch to the safe position and every switch must be restored to the operating position before voltage can be applied to the generators. Indicating lamps on the control desk show the position of these various switches. In addition, it is the operator's duty to see or otherwise account for all the individuals working around the yard before energizing the equipment.

The station is equipped with all the usual protective relays as well as photocells which clear the circuits and deenergize the generator fields in case of a power arc anywhere in the station. Furthermore, an observer is stationed in a soundproofed booth in the generator room. He has control of an emergency switch which can be tripped in case of any trouble in the station. The observer also changes the reactor switches when changes in current are required, but, as was stated previously all control of the plant is located at the control desk.

Air-closing switches are used instead of breakers for initiating the short circuit, when opening tests on breakers are being made. These switches can start the short circuit at any desired point on the voltage wave, thus controlling the degree of current asymmetry on single-phase tests. As the closing time of these switches must remain constant, they are spring-closed and are operated by a solenoid mechanism which latches the switches in the open position. Synchronized closing is accomplished by tripping a grid-glow tube from a small impulse generator adjustably coupled to the main generator shaft. The grid-glow tube energizes the unlatching coil on the closing switch. As the operating time is constant within 0.0015 second, a close control of current asymmetry is possible.

In the control room are located (Figure 7) sequence drums which control the actual operations for a test, once the proper series reactance has been chosen and the generators excited to the proper voltage. In a normal opening test, the sequence drum starts the oscillograph, closes the closing switches, trips the test breaker, trips the generator backup breakers and trips the generator fields. In case of a close-open test, the operator closes the closing switches before starting the sequence drum and the drum closes and opens the test breaker.

Two sequence drums are provided, making it possible to test in two locations simultaneously. For instance, one generator may be used for a test in one of the 13.2-kv cells while the other is used for a test in the high-voltage cell. Adjustment of the time interval between the functioning of the various devices controlled by the sequence drums is accomplished by moving the cams with respect to each other. The cam-operated switches are connected to plugs as shown in Figure 7, and all essential controls are brought to jacks on the panels above the sequence drums. This arrangement provides great flexibility of control.

Measurements

Magnetic oscillograph records are almost invariably made of all tests. Figure 8 shows the oscillograph table (also located in the control room). Two multielement oscillographs are available and when desired, they can be operated as a single unit thus making available a total of 18 records. All potential and current transformer secondaries are brought to a plug board above the oscillograph table. Current transformers are used for all current measurements, specially designed units being provided to assure accurate current records. Potential transformers are located on the 13.2-kv bus supplying test cells 3, 3*A*, and 4. No potential transformers are supplied in the high-current cell. When required, portable units are used. Currents in the high-current transformers are measured on the primary or 13.2-kv side. Several relays are available from each test cell to the oscillograph table to take care of travel records, pressure indicators, and any other desired measurements.

A cathode-ray oscillograph of the cold-cathode type for measuring recovery-voltage transients is located on the second floor of the control house. An overhead transmission line provides the connection between the oscillograph and the test cells.

Transient Recovery Voltage

The severity of circuit-breaker testing depends not only on the voltage and current of the circuit but also on the rate at which the voltage appears across the breaker contacts during the recovery transient. The layout of the laboratory with the reactors close to the test cells makes it possible to obtain high natural frequencies, and values up to about 200,000 cycles per second have been recorded. Transients and breaker performance on these test circuits can be the equivalent of those obtained under severe service conditions.

Observation

A limited number of witnesses can be accommodated in the control room, but for the larger number of observers present for a demonstration test, a large room affording a good view of the test cells is provided on the second floor of the control house.

Operating Experience

The location of the high-capacity testing station within a manufacturing plant has been justified by sixteen years successful operation and the close effective co-operation thus obtained among the design, manufacturing, and test departments.

This high-capacity testing laboratory is in effect a power station capable of delivering, momentarily, a short-circuit output comparable with that obtainable on large power systems. In contrast, however, it carries no continuous load, and a short circuit is a normal instead of an abnormal condition. The flexibility of the testing station permits the duplicating of many voltages and currents corresponding to different service classes and ratings of equipment. Accurate control of the conditions and complete records of performance produce experimental data which, when properly used, result in circuit breakers and other equipment of adequate design and proven ratings.

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Electrical Drives for Wide Speed Ranges

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THE electrical drive for a wide speed range is obtained by the addition of a rotating regulator called a Rototrol to a conventional variable voltage system. This combination will give a speed range of 120 to 1 or more, and can be used effectively in many industries to simplify the mechanical design of the machine which it drives. Its use eliminates elaborate gear-change mechanism, clutches, and so forth, and at the same time gives a more flexible control scheme with the complete speed range under the control of the operator without leaving the work or stopping the machine.

The conventional variable voltage or Ward-Leonard scheme of control is the accepted method of obtaining speed ranges in excess of 6 to 1 with 20 to 1 about the maximum range. This range is obtained by combination of motor-field and generator-voltage control. The amount of field control is limited by motor stability, and the amount of voltage control is limited by speed regulation and maximum torque. The addition of regulating devices and other refinements to improve the characteristic of this scheme make it possible to extend the range into the field that can be defined as a wide speed range.

The increase in speed range has been accomplished by widening the range of voltage control, the range in field control remaining not more than 4 to 1 and preferably only 2 to 1. Therefore this type of drive is best suited to a load which has constant torque characteristics. The machine tool industry has many applications of this type, and it is here that the wide-speed-range drive has been applied. Feed drives on boring mills, milling machines, automatic screw machines, and so on, have constant torque characteristics as the load consists mostly of overcoming friction of the moving parts. Wide ranges of feed speed are required to satisfactorily machine the wide variety of sizes and kinds of metals. With the wide-speed-range drive, these speeds are always immediately available to the operator without the necessity of leaving the work and stopping the machine to change gears. Speed regulation is good, and when desired the rheostats can be calibrated to read feed speed directly.

To develop a satisfactory wide-range variable-voltage drive it has been necessary to compensate for certain characteristics at the low speed that are not important at high speeds. The two most important factors are the residual voltage of the generator and the *IR* drop of the system.

Most commercial generators have a residual voltage of between three and four per cent. This would mean that the no-load speed of a motor which is being driven from such a generator by variable-voltage control is limited to a no-load speed range of about 25 to 1, since the residual will not permit a voltage lower than itself to exist. If it is desirable to extend the range beyond 25 to 1, it becomes necessary to make some arrangement to overcome the residual voltage. Also, since the residual is a function of the previous magnetic history of the generator, it is necessary that the equipment used to overcome residual must be able to determine the actual conditions which exist.

The speed of the d-c motor will normally drop as load is applied due to the *IR* drop in the system. Since a feed mechanism must run at constant speed regardless of the load, it is necessary that the speed-torque characteristic of the motor driving the feed be practically flat. Since the normal drop on a d-c motor is due almost entirely to *IR* drop, some arrangement must be made to increase the applied voltage by the amount of the *IR* drop as the load is applied so that the induced voltage, and therefore the speed of the motor, will remain constant. These requirements would indicate that some means of regulation must be supplied in order to maintain the desired characteristics. It has been found by experience that a rotating regulator or regulating generator is satisfactory for this particular function and has been widely applied.

This type of rotating regulator was first developed for elevators and a large installation was made in Radio City in 1934. Since that time the Rototrol has been used in many industrial applications. It is the heart of the variable-voltage planer drive and has been proven in service over a period of several years. Other applications include paper mill drives, electric shovels, and skip hoists. In appearance it looks the same as a standard d-c generator and is usually mounted as a unit of the variable-voltage motor generator set.

In the use of any regulator which is to vary the voltage of a generator, it is possible to have the regulator take care of only the changes which are required, or to incorporate in it the total excitation of the machine to be regulated. Where very great voltage range is required and where the response should be quick and accurate, we have found it desirable to incorporate in the rotating regulator only the regulating functions which are desired and to supply the normal excitation from some other source. The most satisfactory method to supply the regulating current to the fields of the generator is by use of a Wheatstone bridge circuit in which the armature of the rotating regulator is placed where the galvanometer in the conventional bridge is located. This is shown in Figure 1.

In this circuit generator fields *GF*1 and *GF*2 are identical as are regulator fields *RF*1 and *RF*2. Resistors *R*1 and *R*2 are equal to each other and to the sum of the resistances of a regulator and a generator field. Thus all four legs of the bridge are of the same resistance, and a balanced bridge results.

It can be demonstrated that in a balanced-bridge circuit of the type described when a source of voltage is connected to points *A* and *C*, points *B* and *D* have the same potential, and consequently no current from said source tends to flow through any circuit connected between *B* and *D* such as the armature of the regulator. The converse is also true that a source of voltage applied to points *B* and *D* will not cause any current from said source to circulate in a circuit connected to points *A* and *C* such as the exciter circuit.

It can also be demonstrated that in a circuit as described, if the exciter voltage applied alone to points *A* and *C* circulates *Ia* amperes in each leg *A-B* and *C-D* (containing the variable-voltage generator and regulator-field windings) and if the regulator armature voltage applied alone to points *B* and *D* circulates *Ib* amperes in each leg *A-B* and *C-D*, then when the two voltages are applied together they will circulate in each leg *A-B* and *C-D* a current equal to the algebraic sum of *Ia* and *Ib*. Thus the use of a balanced-bridge circuit permits complete independence of

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the excitation and regulating currents even though they have common paths in the field circuits of the generator.

Figure 1 shows a schematic diagram of the equipment required for a wide-speed-range feed equipment for a machine tool. For the purpose of simplicity many of the control elements such as operating coils for the various contactors, control stations, and so on, have been omitted as they are not required for this discussion. It can be seen that this circuit is basically the variable-voltage system except for the addition of the regulating generator and its associated circuits. These additions, however, extend the range of speed adjustment and give much better regulation than can be obtained with a straightforward variable-voltage system. The generator differential field *GF*3 and its contactor *BR* are a part of the variable-voltage scheme which prevents creeping in the off position and are not involved in obtaining the wide speed range.

If the generator-field rheostat be set at some position and the forward directional contactors *F* be closed, current will flow from the exciter through the two branches *ABC* and *ADC* of the bridge. This current, *Ia*, flowing through the generator fields *GF*1 and *GF*2 will cause the generator to produce a voltage across its armature and thus across the motor armature. Since the motor field is excited, the motor will run at the speed determined by its field strength and its applied armature voltage, assuming no load and thus no current and no *IR* drop in the armature circuit.

The current *Ia* flowing in the regulator fields *RF*1 and *RF*2 will set up a magnetomotive force in the regulator which will cause it to generate a voltage which will tend to circulate a current *Ib* in the direction so as to strengthen the fields of the generator and regulator in the bridge circuit.

This magnetomotive force is not permitted to act alone in the regulator but is balanced by the magnetomotive force of field *RF*3 which is connected so as to be differential to *RF*1 and *RF*2. Its strength is adjusted by resistor *R*5, so that at this condition of operation, its ampere-turns are equal in magnitude to the ampere-turns of *RF*1 and *RF*2. Thus the fields in the bridge circuit get a cue as to the value of voltage which is desired and the field across the armature circuit measures the actual voltage which is obtained. Since at no load the speed of the motor is determined by its applied voltage, we might consider these two regulator field circuits to compare actual speed with desired speed.

If the calibration of the fields has been made at the point being described then the resultant regulator magnetomotive force is zero and no voltage is present across its armature terminals and thus the regulating current *Ib* is zero.

Let it be assumed, however, that this condition of operation being described is not at the calibration point but at some other value of speed, and that, due to shape of the saturation curve or to effect of the hysteresis loop of the generator, the voltage generated is not as great as it should be to give the motor speed desired. Then the differential ampere-turns in *RF*3 are not so great as the cumulative ampere-turns in *RF*1 and *RF*2, leaving a resultant ampere-turns which cause the regulator to circulate current *Ib* in the direction shown in Figure 1. This current causes an increase in actual generator-field current, as shown earlier in this paper, and thus causes the generator voltage to increase to the value it should have.

To maintain this regulating current at the necessary value, the constants of the circuit consisting of the regulator armature, the resistances of the bridge circuit, and the regulator fields *RF*1 and *RF*2 must be such as to make the circuit self-energizing. This can be done by adjusting resistor *R*3 so that the field characteristic line of the combined effects of fields *RF*1 and *RF*2 will be coincident with the air-gap line of the no-load saturation curve of the regulator generator for those fields alone. Thus the regulator will force enough regulating current *Ib* through the generator fields until the proper operating point is reached. At this time the ampere-turns due to the

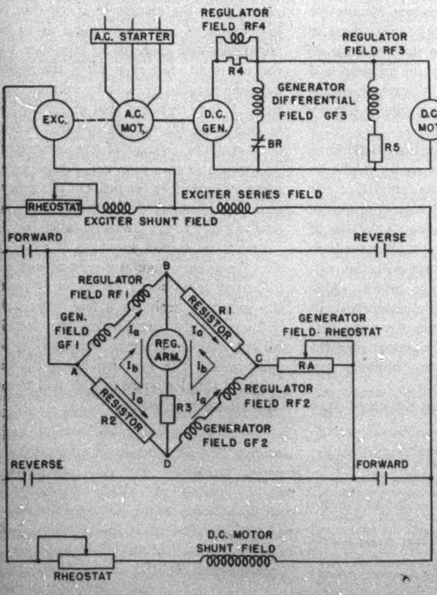

Figure 1. Schematic diagram of main circuits of the wide-speed-range drive

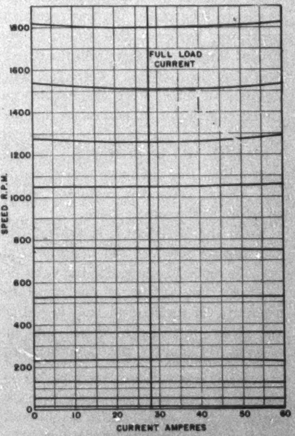

Figure 2. Speed regulation curves covering a range from 15 to 1,800 rpm

Maximum speed regulation at any speed is less than five per cent

Figure 3. Typical variable-voltage motor-generator set showing the units used in a wide-speed-range drive

This is a four-unit set with a main generator, exciter, and regulating generator (on the right-hand side) driven by a squirrel-cage induction motor

Figure 4. This horizontal boring, milling, and drilling machine has an electrical feed drive with a speed range of 120 to 1

The electrical drive eliminates the use of change gears, clutches, and so on, and greatly simplifies both the construction and manipulation of the machine

current from the exciter Ia in the fields $RF1$ and $RF2$ will be balanced by the ampere-turns in field $RF3$ due to the proper voltage across the armature of the generator and the regulator current Ib through fields $RF1$ and $RF2$ will just maintain the current Ib.

It should be noted that if the regulating generator is not self-energizing that it would not be possible to get a correct operating point, since to have any corrective current circulated there would have to be some difference in the cumulative ampere-turns due to Ia and the differential ampere-turns due to voltage. In such a case the voltage would approach but never reach the correct value and the accuracy of regulation would be greatly decreased.

An ordinary generator when set up with a self-energizing field under the conditions above described would tend to build up to the point at which saturation begins to take place. In this regulating generator, however, this action is prevented since any charge from the correct operating point causes a corrective magnetomotive force, since the balance between cumulative ampere-turns and differential ampere-turns in the three regulator fields will be destroyed. Analysis shows that the difference which results will always cause the regulator output to return to the correct value.

From the foregoing it is evident that the no-load speed of the motor will always be made to agree with the value desired by the setting of the generator rheostat. If mechanical load is put on the motor, it will slow down, thus decreasing its counter electromotive force and thereby permitting a current to flow in the armature circuit of the two machines. A stable point of operation occurs when the drop in speed and counter electromotive force is just sufficient to make available enough of the generated voltage to circulate the current required.

To bring the speed back to the no-load speed necessitates the raising of the generator voltage by the amount of the IR drop. This is done by the regulator by adding on its magnetic structure another field $RF4$, which is connected in the armature circuit so as to produce cumulative ampere-turns. By proper adjustment of $R4$ this field can cause the regulator to correct almost perfectly for IR drop and armature distortion of the motor over a wide range of load thus giving a very flat speed-load curve.

Other conditions than load tending to change the speed of the motor might be thought of, but it will be found that the regulator generator will make appropriate corrections. Thus burning or shifting of the brushes of the generator often tends to cause stalling of the motor with the conventional variable-voltage drives. Also varying values of residual magnetism in the variable-voltage generator cause different voltages for the same rheostat setting. Such conditions however are corrected by the regulator.

In Figure 2 are shown the speed–load characteristics as obtained from actual tests on a $7^{1}/_{2}$-horsepower machine-tool feed equipment using a regulating generator such as just described. These curves show the regulation of the motor to double full-load current over the range of from 15 rpm to 1,800 rpm or a speed range of 120 to 1. Of this range, speeds up to approximately 1,000 rpm are obtained by variable-voltage control and from 1,000 to 1,800 rpm by motor-field control.

It can be seen by reference to these curves that the change in speed due to application of load is so small as to be negligible, and there is no tendency for the motor to stall even when the speed has been reduced to $^{1}/_{60}$th by voltage control. It is to be noted that at light loads at this speed the voltage required is less than residual voltage of the generator. The regulator has, however, caused a reversal of current in the fields of the generator and thus bucked the voltage down to that required to get the speed called for.

From the results shown it is evident that the use of a regulator scheme such as indicated gives speed range far beyond the supposed limits of a few years ago without greatly complicating the electrical apparatus or going to laboratory-type equipment. The machine being driven can be a simpler, more flexible and more efficient machine tool.

Measurement of Maximum Demand

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Synopsis: "Maximum demand" has become an increasingly important item in rates for the sale of electric service since the appearance of Doctor John Hopkinson's notable paper, "Cost of Electricity Supply," in the year 1892—nearly 50 years ago. The writer estimates that the demand-measuring equipment which is now being used by the public utilities of the United States has a value of the order of $100,000,000. This is an index of its present importance. The object of the present paper is to describe in detail the thermal-storage method of measuring the maximum demand of a user of electric service. A comparison is made between this method, which at each instant of time indicates the logarithmic average load over some nominal time interval, and the commonly used "block-interval" method, which indicates the arithmetic average load over the same time interval. Also, the effect of using a modified design of thermal-demand meters is discussed. The writer has contributed a number of previous papers dealing with this same general subject, reference to which will be found at end of paper.

The Block-Interval Meter

As is well known, the commonly used block-interval demand meter measures the arithmetic average load over some definite interval of time. Its construction and operation are such that, at the end of the month, the maximum arithmetic average block of load over some definite time interval is indicated on the meter scale. The meter is then reset to zero so that the maximum arithmetic average load for the following month may be made available when the meter is again read and reset at the following month's end. As is well known, the commonly used "block-interval" demand meter measures this maximum arithmetic average load over some definite interval of time, such as 5 minutes, 10 minutes, 15 minutes, 30 minutes, and occasionally 60 minutes. Other time intervals have been proposed, even as low as one minute, but so far as the writer knows, no time intervals of less than 5 minutes nor more than 60 minutes are actually being used at the present time. The 15-minute time interval is the one most frequently used, followed by 30 minutes and 60 minutes. Knowlton, in his "Electric Power Metering," says, "A survey by the National Electric Light Association rate research committee in 1931 showed the 15-, 30-, and 60-minute intervals to be in vogue in the ratio of 40:8:1 respectively." Knowlton does not mention any time intervals shorter than 15 minutes, although to the writer's certain knowledge time intervals as short as one and two minutes have been used in the past. To the best of his knowledge the five-minute time interval is still being used to a limited extent.

Basic Weakness of Block-Interval Meter

As mentioned in the preceding paragraph, the "block-interval" demand meter measures the maximum arithmetic average load over some definite interval of time. The arithmetic average, as measured by the standard block-interval demand meter, is a discontinuous function of time. Any arithmetic average must necessarily be a discontinuous function of time, and, as such, be subject to a possible error of 50 per cent—a possibility which can become a certainty at the option of the service user. When it becomes necessary to use two or more block-interval demand meters on the same load, it is customary to synchronize the meters so that they reset at the same instant. This synchronization has no effect on the inherent inaccuracy of the block-interval demand meter, but it does prevent the utility and the utility's customers from becoming aware of the inaccuracy.

While we are considering possible inaccuracies of measurement when using the standard block-interval demand meter, a situation which recently came to the writer's knowledge may be of interest—a situation the possibility of which is mentioned in the preceding paragraph. This service user, as reported to the writer, is taking deliberate steps to "split peaks." His loading schedule is so timed that his demand meter resets, as closely as possible, midway during load applications. If this method of taking load is carried out to its possible extreme, the maximum demand measured and billed can be made one-half the actual arithmetic maximum demand. Such a method of taking service is, of course, open to any user of service whose maximum demand is measured by means of the standard block-interval demand meter. In the case of deliberate "peak splitting," the utility is always on the losing end.

Maximum demand may be accurately measured only by a continuous function of time. Further, even if it were possible so to modify the demand attachment of the standard block-interval meter and thereby obtain a true and accurate arithmetic average, the maximum arithmetic average thereby obtained would still be defective. For instance the maximum arithmetic average—or logarithmic average—of a user of electric service who takes a perfectly steady load of 100 kw would, of course, be 100 kw, and his bill for service would be based on 100-kw demand. Another user whose maximum demand is measured by means of a standard 30-minute block-interval meter and who takes during each half hour 100,000 kw for 1.8 seconds, or 10,000 kw for 18 seconds, or 1,000 kw for 180 seconds (3 minutes), or 500 kw for 6 minutes, or 300 kw for 10 minutes, or 200 kw for 15 minutes, would have exactly the same arithmetic average as the first user—provided there were no "peak splittings." This latter group of cases certainly warrants a higher maximum-demand assessment than the first user. Also, the earlier mentioned members of this group warrant a higher maximum-demand assessment than the later mentioned members.

The Thermal-Meter Principle

On the other hand, the thermal wattmeter measures the logarithmic average of the load under measurement. (For a definition of the term "logarithmic average," see reference 1 at end of paper.) There is a basic difference between the logarithmic and arithmetic averages. The logarithmic average is a continuous function of time. That is, at each instant of time, it indicates the logarithmic average over the immediately preceding 15-minute, 30-minute, 60-minute, or other time interval. The length of this time interval is dictated by the value assigned to an adjustable constant *k*, the character of which is discussed later in this paper. The load taken by a user of electric service is continuous—but not necessarily steady—and its maximum demand can be consistently measured only by a continuous function of time. Even if it were possible to measure the maximum arithmetic average accurately—which is not the case with any of the existing types of block-interval meters—the thermal type would still be preferable, since the heating effect

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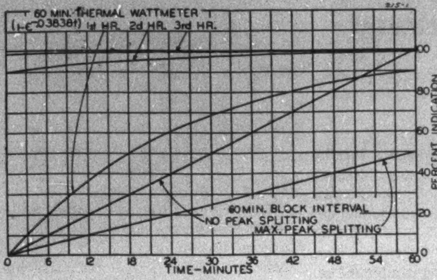

Figure 1

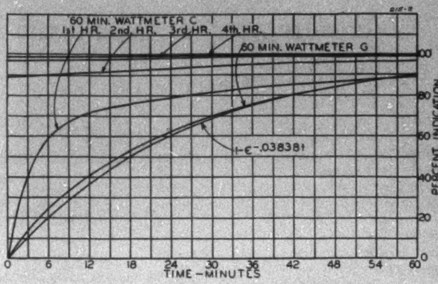

Figure 2

of any given load on the equipment serving the load is a logarithmic or exponential function of time and not an arithmetic function.

While the thermal wattmeter has been fully described in previous papers (see references 1 and 2), it might be well to give a brief description of its operation. A small transformer whose primary is connected across the supply voltage causes a current to circulate continuously through two equal resistances, which we will call r. Let us call this circulating current E, since its value is always proportional to voltage. The load current is caused to pass through these same resistances. Let us call this load current I. The load current is caused to pass through the resistances in such a manner that one-half its value adds to the circulating current in one resistance and subtracts from it in the other. The total current in one resistance is, therefore, $E+I/2$ and in the other $E-I/2$. The total heat applied to one resistance is, therefore $\left(E+\frac{I}{2}\right)^2 r$ and to the other $\left(E-\frac{I}{2}\right)^2 r$. The thermal wattmeter is so constructed that it constantly indicates the difference in temperature between two masses of matter which are being heated by these two currents. The difference in heat applied is obviously $2EIr$, a function which is always directly proportional to the watts entering the load. Since the masses of matter do not and cannot respond instantly to the heat applied, the result is a demand meter measuring the logarithmic average load.

Mathematical expressions for the indications of the two types may readily be obtained. For the arithmetic average (block-interval) meter, the mathematical expression for its indication is

$$\text{Meter indication} = \frac{1}{t_1-t_2}\int_{t_2}^{t_1} w\,dt \qquad (1)$$

The logarithmic average may be expressed mathematically as follows:

$$\text{Meter indication} = k\int_0^t w\epsilon^{-kt}dt \qquad (2)$$

In the above expressions

w = instantaneous value of watts
t = time
k = an adjustable constant
ϵ = base of Napierian logarithms
t_1-t_2 = time over which arithmetic average is measured

Figure 1 gives the solution of these two equations when the time interval for demand measurement is 60 minutes—the maximum time interval now being used. The relative indications of the two types for load durations up to 60 minutes may readily be seen from Figure 1.

As actually constructed, however, the relative indications of the thermal-demand meter may be made to depart radically from the purely theoretical ratios shown in Figure 1, or may be made to conform closely to it, depending on design. This comes about because the diffusion of the heat that necessarily enters or leaves the working parts of the thermal wattmeter during normal operation does not and cannot take place instantaneously. As is well known, heat does not and cannot diffuse instantly throughout any mass of matter that is being heated or cooled. The process of heat diffusion takes time. The values given in Figure 1 take no cognizance of this. Also, this factor depends to a marked extent on the design of the thermal wattmeter. This matter is treated further later in this paper.

Referring to mathematical expression 2, it might be pointed out that when the value of kt reaches 6.9078, ϵ^{-kt} becomes one-tenth of one per cent of what it is when $kt=0$. One-tenth of one per cent is as small a quantity as can be read accurately on the demand-meter scale. Therefore, while the integration of the thermal wattmeter extends from zero to infinity, the only significant part of this integration is that which takes place during the interval of one or two hours—or at most three hours—immediately preceding the instant of observation. This holds true when $kt=2.3026$, the value assigned to kt in the edition, 1941 Code for Electricity Meters, the code sponsored by the American Standards Association. This also assumes that the time interval for demand measurement shall not exceed one hour. If the time interval for demand measurement is 15 minutes, the time interval most frequently used in the United States, the only time interval of any significance is the 45 minutes immediately preceding the instant of observation.

Demand Measurement—a Problem in Transients

The measurement of maximum demand is a problem in transients unless the load during maximum is perfectly steady. If the loads taken by users of electric service were always steady, the standard block-interval demand meter would measure maximum demand with perfect accuracy. The arithmetic and logarithmic averages of a steady load are exactly the same. An unvarying load requires no demand meter. Under the conditions of the actual use of service, the only existing method of assuring accuracy of measurement is by the use of a continuous function of time. (The thermal wattmeter is an example.) Our usual conception of a transient is the phenomena that occur in a stroke of lightning or a power interruption. Anyone who dealt with the problem of transients is, of course, aware that time must appear as an exponential function, as it does in the thermal wattmeter. When dealing with thermal wattmeters, the time involved, instead of being of the order of microseconds, as in the usual type of transient, is now of the order of minutes, or even of hours.

Comparison of Block-Interval and Thermal-Demand Meters

Let us assume a case where a service user is taking a continuous steady load of such a value that it causes the demand meter to reach a value which we will call 100 per cent. Let us assume further that this load is being measured by a standard 60-minute block-interval demand meter and also by a 60-minute thermal-demand wattmeter. Let us assume further that the thermal wattmeter is the 60-minute wattmeter *G* which is described later in this paper. If this user's load is perfectly steady, both wattmeters will indicate 100 per cent demand. If however, business falls off to such an extent that it is necessary to use service only during alternate hours, Figure 6 shows the relative indications of the two demand meters. The data shown in Figure 6 are the results of a carefully made test. Inspection of Figure 6 shows that the indications of the thermal and block-interval types cross at approximately 52 minutes when there is no peak splitting. Therefore, if the time of load duration is less than approximately 86.5 per cent of the nominal time interval, the logarithmic average is always in excess of the arithmetic, whether peak splitting occurs or not. In this case, also, the utility using the block-interval meter is always on the losing end.

Ithaca Tests

In order to get some definite information on the question of how much difference might be expected in the indications of the two types of demand meters on actual loads, there has been conducted in the city of Ithaca, beginning in March 1939, and continuing for about 18 months, a series of tests which may be of interest. Ithaca, at that time, was using a 15-minute demand interval, since then increased to 30 minutes. For each of four customers, three demand meters were installed. These four customers were two garages, a beauty parlor, and a photographic studio. Number 1 meter was a 15-minute block-interval meter of standard make. Number 2 meter was an identical block-interval meter so adjusted that its time interval "broke joints" with number 1. That is, number 2 meter reset $7^{1}/_{2}$ minutes after number 1. Number 3 meter was a 15-minute thermal-demand wattmeter. For a time, there were two thermal wattmeters on some of the loads. This was done to show by direct experiment what theoretical considerations tell us; that is, that the logarithmic average, being a continuous function of time, must always be the same on any given load. In no case was any material difference found between the two thermal wattmeters.

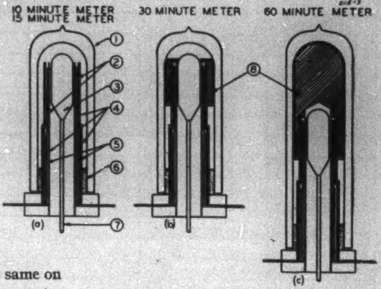

Figure 3. Diagrammatic cross section of 15-minute, 30-minute, and 60-minute thermal wattmeters

1—Thermos bottle
2—Heater resistances
3—Reservoir
4—Insulating tubes
5—Heater resistance leads
6—Felt packing
7—Capillary tube
8—Mass

Now, comparing the two block-interval meters, if we exclude one monthly reading, which for some unknown reason—presumably failure to reset—showed a higher difference than theory can account for (240 per cent), the maximum difference between the two was 27.3 per cent. From this, the difference ranged down to 0 per cent. In only six cases out of about 60 monthly readings did the two block-interval meters give the same indications. The average difference for the entire 60 monthly readings was 10.1 per cent. In some cases number 1 meter indicated the higher value, and in other cases number 2. This difference in indication is, of course, due to "peak splitting" either by number 1 or number 2 meter, or perhaps by both.

Now comparing the thermal-wattmeter readings with the two block-intervals, in only one case out of about 60 monthly readings did all three demand meters indicate the same values. In some cases, the thermal meter indicated higher than either block interval, in some cases, lower than either, and in still other cases, between the two block intervals. (For other comparisons between the two types see references 2, 4, and 6.)

When a public utility deals with its customers' pocketbooks, the degree of inconsistency shown by these Ithaca tests is, in the writer's opinion, intolerable. Accuracy in the measurement of kilowatt hours is and always has been extraordinarily high. Unfortunately, the same cannot be said of the measurement of maximum demand as evidenced by the Ithaca tests.

Advantage of Bourdon Tube Over Bimetal Strip

In the thermal wattmeter of the past, the means by which temperature difference has been indicated has been the bimetal strip. In the year 1924, Chester W. Rice of Schenectady contributed a paper to the AIEE entitled, "Free Convection of Heat in Gases and Liquids—II" (see reference 9). In this paper he showed that when the loss of heat from a hot body occurs by free convection, the rate of heat loss is not directly proportional to temperature elevation, but to the temperature elevation raised to 1.25 power. The writer did not become aware of this important contribution to our technical literature until about 1927. For many years previously, the writer had noted an error in the thermal wattmeter on low-power factors, for which he could give no satisfactory explanation. As soon as Mr. Rice's 1924 paper came to his attention, the reason for this error became obvious at once. In the bimetal-strip type of thermal wattmeter, practically all the heat escapes by free convection. The bimetal-strip type of construction does not lend itself to any means of heat escape except free convection. The only answer, therefore, seems to be to adopt some means of heat escape other than free convection. To accomplish this end, it was necessary to consider means of temperature detection other than the bimetal strip.

The writer therefore decided to try out the Bourdon tube. He had previously experimented with the Bourdon tube, but without success. Beginning about 1928, the writer began experimenting with Bourdon tubes in earnest. By about 1934, the Bourdon tube method of measuring maximum demand had arrived. A complete description of the Bourdon-tube demand wattmeter was contributed to the AIEE in 1935 (see reference 5).

The Constant k and Its Determination

Mathematical expression 2 given above brings into the picture of demand measurement the value of the adjustable constant *k*. There is a direct relationship between this matter of time interval and

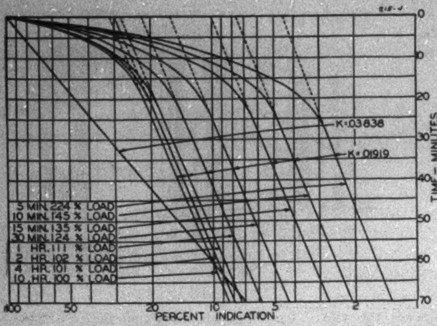

Figure 4. 60-minute thermal wattmeter C, zero-load performance

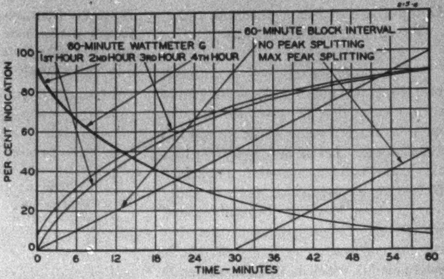

Figure 6. Comparison of 60-minute thermal and block-interval wattmeters

the value of this adjustable constant k appearing in mathematical expression 2. The existing Code for Electricity Meters fixes the time interval for thermal-demand meters as the "time required for the instrument to indicate 90 per cent of the full value of the steady load, which is thrown suddenly on it." This provision fixes the value of kt in equation 2 at $kt = 2.3026$. This, in turn, fixes the value of k at that value which is obtained by dividing 2.3026 by 5, 10, 15, 30, 60, or whatever value of time interval the utility has adopted. Table A gives these values.

Effect of Heat Diffusion on Constant k

When we apply a given constant value of watts w to a thermal wattmeter in accordance with the procedure specified in the Code for Electricity Meters and take readings at equal intervals of time, the function we observe is the function $w(1-\epsilon^{-kt})$. The response for the two thermal wattmeters C and G shown in Figure 2 were obtained in this manner. If, when the meter indication has reached some value w', we throw the load entirely off and observe meter indications as they approach zero, the function we observe is $w'\epsilon^{-kt}$. This, a pure exponential function, plots as a perfectly straight line on semi-log paper. However, when we plot these data from the thermal-demand meter, we find a departure from a straight line, the amount of this departure being controllable by design. Figure 4 shows the performance of 60-minute wattmeter C with zero load, and Figure 5 that of 60-minute wattmeter G. Examination of Figures 4 and 5 makes it obvious at once that the value of k is not constant, but can be made to vary over a very considerable range during the first few minutes of the application or absence of load.

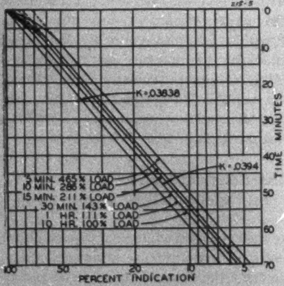

Figure 5. 60-minute thermal wattmeter G, zero-load performance

The reason for this anomaly of the variable constant is not far to seek and has already been intimated. Heat cannot be made to diffuse instantly throughout any mass of matter. In the thermal wattmeter, the source of heat is, of course, the heater shown in cross-section in Figure 3. In order to actuate the meter, the heat originating in the heater must reach the reservoirs shown in Figure 3. From the data given in Figures 4 and 5, the time required for the rate of heat flow to become constant varies over a very considerable range. From Figure 4, it is obvious that this rate of heat flow in wattmeter C does not become constant for some 20 to 30 minutes. From Figure 5, it is evident that the rate of heat flow for wattmeter G becomes constant in considerably less than 10 minutes. About five or six minutes would closely fit the experimental data seen in Figure 5. In other words, after a change in load occurs, k is not a constant, but a variable, and does not become a constant for some 5 to 30 minutes, the length of time being subject to meter design.

Table A

Time Interval	60	30	15	10	5
k	0.03838	0.07675	0.1535	0.23026	0.4605

Time Interval—How Long?

What is the proper time interval to use in the measurement of maximum demand? As indicated at the beginning of this paper, United States public utilities are using various time intervals in the measurement of maximum demand. It is obvious that the longer the time interval used in demand measurement, the less is the probability of a steady load during that time interval, and, therefore, the greater is the probability of a difference between the logarithmic and arithmetic averages. The 15-minute time interval is by far the most frequently used. The shorter time intervals have been brought about by the desire on the part of public utilities to obtain adequate compensation from those service users whose loads are inherently of the short-time, high-peak variety, such as welding, short-time heating, shovels for excavation purposes, and so on. It might be pointed out that the thermal-demand meter automatically recognizes the short-time, high-peak demands in this class of loads. Inspection of Figure 1 shows this clearly. The thermal-demand meter—even assuming instantaneous heat diffusion—gives higher demand indications on short-time loads, approximately 2.3 times that of a block interval of the same time rating. The phenomenon of heat diffusion increases this ratio still further.

Examination of the data in Figure 2 shows that if we use 60-minute wattmeter C, its indication on short time loads (load durations of a minute or two) is approximately equivalent to a five or six minute block interval. This ability of the 60-minute thermal-demand wattmeter to read high on short-time loads may be still further emphasized by suitable design, if the users of demand meters so desire. It should be pointed out, however, that if the design be so altered as to make its short-time response equivalent to a two- or three-minute block interval, the rate of response for load durations of more than 90 minutes is so slow that it would require some 12 or 15 hours of perfectly steady road to cause the meter to reach 99.9 per cent of its final indication. In the writer's opinion, the modification of the 60-minute thermal wattmeter's design to the point where it is equivalent to a five- or six-minute block interval is as far as the modification should be carried. Even this modification has reduced the permanent value of the constant k to one-half that of the 60-minute thermal wattmeter when heat diffusion is assumed to be instantaneous—$1-\epsilon^{-0.03838t}$. Also, it is highly questionable in the writer's opinion, whether or not wattmeter *C*, while it meets the meter-code specifications, can honestly be called a 60-minute meter.

A 60-Minute Meter

On the assumption that if the time interval for demand measurement is ever standardized, such standardized time interval would not exceed the maximum time interval now used—60 minutes—the writer has been experimenting with 60-minute thermal wattmeters. The most obvious way of obtaining a 60-minute thermal meter is to add mass to the 15-minute meter, as shown in Figure 3, so that the time to heat this mass up to 90 per cent of its final temperature is increased from 15 minutes to 60 minutes. When a steady load was applied to this 60-minute meter, wattmeter *C* of Figure 2 shows the test results. It will be noted that when a steady load was applied to this 60-minute wattmeter *C*, it reached 50 per cent of final value in approximately 4 minutes, 60 per cent in approximately 6 minutes, 70 per cent in approximately 10 minutes, 80 per cent in approximately 26 minutes, and 90 per cent in approximately 60 minutes. The design of the meter was then modified so that the mass to heat and cool during normal operation was reduced to a small fraction of that necessary in meter *C*. Wattmeter *G* of Figure 2 shows the test results on this redesigned 60-minute meter. It will be noted that wattmeter *G* reaches 50 per cent of its final in approximately 17 minutes, 60 per cent in approximately 23 minutes, 70 per cent in approximately 30 minutes, 80 per cent in approximately 42 minutes, and 90 per cent in approximately 60 minutes.

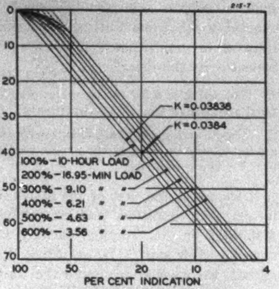

Figure 7. 60-minute thermal wattmeter I, zero-load performance

Let us examine this matter a little further from a purely theoretical standpoint. When we integrate mathematical expression 2 above, the result of this integration is, of course, meter indication$=w(1-\epsilon^{-kt})$. With large values of time (t), ϵ^{-kt} approaches zero and meter indication approaches w (watts). If $t=$infinity (steady load), meter indication $=w$. If, however, t is of a small value making it necessary to use the limits of integration, meter indication$=\dfrac{w(1-\epsilon^{-kt})}{\text{per cent time}}$. In the foregoing sentence, the adjective "small" may be defined as any value of t (time in minutes) such that the product kt does not exceed 6.9078.

After a change in load occurs, wattmeter *C* acquires approximately 80 per cent of the value of this change during the 20 or 30 minutes k is a variable. The remaining 20 per cent requires approximately five hours additional time. Wattmeter *G* acquires approximately 20 per cent of final indication in the five or six minutes that k is a variable while the remaining 80 per cent requires approximately an additional three hours.

Now, examining 60-minute wattmeter *G* shown in Figure 2, it is obvious that its response is fairly close to the theoretical value given in equation 2. For very short time loads, its rate of response is some 30 per cent or 40 per cent higher than if heat diffusion could be made instantaneous, but this higher rate of response lasts for only five or six minutes. In this meter, the value of the adjustable constant k is close to the theoretical value of k for a 60-minute meter. Tests on a number of these redesigned 60-minute thermal wattmeters have shown a departure in the permanent value of k of not over two per cent or three per cent from the theoretical value—0.03838. Also, its temporary value is much less than in wattmeter *C*. On short-time loads, load durations of a minute or two, wattmeter *G* is equivalent to approximately a 20-minute block interval. The design of the 60-minute thermal wattmeter is capable of adjustment to almost anything the public utilities may desire between wattmeters *C* and *G* shown in Figure 2. The writer would appreciate an expression of opinion by those using demand meters as to which design shown in Figure 2 they would prefer. Or, perhaps some intermediate design between *C* and *G* would be preferable. It must be borne in mind, however, that wattmeter *C* can be made available for any time interval from 15 minutes up, while wattmeter *G* is available in 60-minute time interval only.

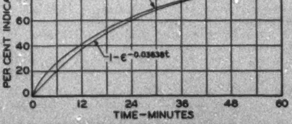

Figure 8. 60-minute thermal wattmeter, three-hour 100 per cent load applied

Statement Re Accuracy

The writer wishes to correct an impression which might be drawn from the preceding discussion. The accuracy of the standard block-interval demand meter in measuring an arithmetic average is as high as human ingenuity can attain, provided we limit this statement to the particular time interval which the meter happens to select. However, we must bear in mind that the particular time interval selected is only one out of an infinite number that might be selected. The writer does not wish his statement concerning the accuracy of the block-interval demand meter to be interpreted in any other manner.

New Data on 60-Minute Thermal Meters

Also, further experimental work has demonstrated that the time interval during which the constant k is a variable can be somewhat reduced below that shown in Figure 5. By taking all possible precautions to make the diffusion of heat as rapid as possible, Figure 7 shows the resulting zero load performance. The test results shown in Figure 7 indicate that the constant k is a variable for a time interval but little over three minutes, even when the load applied is six times full load for 3.56 minutes. Figure 8 shows this meter's performance with full load applied for three hours. The constant k of 60-minute wattmeter I becomes 0.03838 when the two curves shown in Figure 8 become parallel; Figure 8 shows this to occur at approximately three minutes. Also, Figure 7 shows that the permanent value of the constant k departs from the theoretical value by only a negligible amount.

Ammeters Versus Wattmeters

There is one further point that should be considered. Thermal meters are available in both wattmeters and ammeters. These two instruments differ in at least one important respect. The wattmeter scale is uniform throughout its entire range, while the ammeter scale is inherently a scale of squares. Under the existing definition of time intervals, the time interval of both these instruments is defined as the "time required for the instrument to indicate 90 per cent of the full value of a steady load which is suddenly thrown on it." Since the ammeter scale is inherently a scale of squares, this means that the time interval of the thermal ammeter is approximately one-half that of the thermal wattmeter as time intervals are now defined. There are two ways to cure this defect:

1. The value of k for the ammeter may be fixed at one half that of the wattmeter.
2. The percentage of final indication for the ammeter may be fixed at the square root of that for the wattmeter.

If we adopt the first of the above, it means that ammeters and wattmeters will differ in construction, thereby increasing their cost. If we adopt the second, we must either make the wattmeter time interval the time to arrive at 90 per cent of final, as at present, and the ammeter time interval the time to arrive at 95 per cent of final, or we must make the ammeter time interval the time to arrive at 90 per cent of final, as at present, and the wattmeter time interval the time to arrive at 81 per cent of final. It is suggested that those who are responsible for the Code for Electricity Meters give careful attention to this matter and choose one of the above alternatives.

Conclusion

Summarizing, we must obtain the logarithmic average and not the arithmetic average if we would have consistency as well as adequacy in the measurement of maximum demand. Time must appear as an exponential function and not an arithmetic function, if the demand is to be metered in accord with the basic character of the quantity being measured. The writer would also urge that consideration be given to the matter of standardizing the time interval used in demand measurement.

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A New Voltage-Regulating Relay Plus Line-Drop Compensator

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THERE are numerous applications where it is desired to measure a voltage at one point of a line or feeder by the use of an instrument placed at some other point. The instrument measures the voltage at the point of the line to which it is connected, and also measures the line drop to the remote point. These two measurements are combined so that the resultant indication is the voltage at the remote point of the feeder. If the instrument has contacts on it so that it may actuate control circuits, it is known as a voltage-regulating relay plus line-drop compensator. Such a device finds application to regulating equipment such as tap-changing transformers, capacitor-switching schemes for power-factor control, and the like.

Many of the applications for a voltage-regulating relay require a time delay. Usually such delay has been obtained by the use of timers which interpose a fixed lag between the operation of the relay and the device which is being controlled (for example a tap-changing regulator) independent of the magnitude or rate of voltage change. The relay described below eliminates the use of an external timer, since it has its own inherent time delay. In addition, this time delay is not a fixed quantity but varies inversely as the change in voltage. In this way the sensitivity of the regulating device is increased without introducing a large number of unnecessary operations.

The Voltage-Regulating Relay

An induction-type voltage relay with a permanent damping magnet inherently has an inverse time characteristic; hence, such a relay became the basis for design. Time curves for different voltage settings on an induction voltage relay with front and back contacts are shown in Figure 1.

The two major problems to be solved in the completed design were:

1. To make the temperature error negligible, for applications often call for outdoor installations where ambient temperature may vary over an extreme range of 0 to 110 degrees Fahrenheit.

2. To provide the relay with a self-contained line-drop compensator.

Starting with the final result, we may show how these two problems were solved.

TEMPERATURE ERROR

Figure 2 is a wiring diagram of the relay. Neglecting the compensator winding, the principle of operation of the relay is quite familiar.[1] When the potential winding is energized, the transformer winding on the lower pole feeds current to the upper-pole windings. This current induces an upper-pole flux which is out of phase with the potential flux, and a torque on the disk results which satisfies the equation:

$$T = K\Phi_1\Phi_2 \sin a \qquad (1)$$

where Φ_1 is the lower-pole flux, Φ_2 the upper-pole flux, and a the phase angle between them.

There are two sources of temperature error in such a relay:

1. For constant voltage across the relay, change in ambient temperature produces a change in the resistance of the upper-pole circuit. Hence, both the magnitude of upper-pole flux Φ_2 and the phase angle "a" will change. According to equation 1, the torque will, therefore, vary.

2. Change in ambient temperature may also produce variations in relay impedance so that the current in the potential circuit varies with the temperature even though the voltage remains constant.

In appendix I is a derivation which shows that when upper-pole reactance equals upper-pole resistance, temperature error due to variation of upper-pole impedance is a minimum. Accordingly, the upper-pole winding was designed to have a 45 degree impedance angle.

To correct for the second source of temperature error, a swamping reactor is placed in series with the potential coil. Hence, variations in relay burden have little effect on the total impedance of the voltage circuit, so that the current in the relay remains constant independent of temperature. Experimental tests made in a refrigerator and an oven over a range of temperatures from 0 to 110 degrees Fahrenheit showed an average temperature error of less than 0.018-volt per degree Fahrenheit. This ballast impedance method of correction was chosen so that the reactor could serve a dual purpose. Besides correcting for temperature error, it is an essential part of the line-drop compensator circuit. This function is described below.

THE LINE-DROP COMPENSATOR

The line-drop compensators in general use today employ what amounts to an artificial or replica line. That is, the compensator is a variable resistance and reactance which may be independently adjusted to suit a particular line impedance. These elements are placed in series with the voltage-regulating relay, and a current is fed to this compensator from current transformers in the main line, so that the drop across the resistor and reactor is proportional to the line drop and is subtracted from a voltage proportional to the sending-end potential. Thus, the voltage across the relay actually simulates the voltage at the load center, if the resistance and reactance of the compensator have been correctly adjusted.

The relay described in this paper has no such artificial line. Instead the equivalent of two separate torques is superimposed on the induction disk. One of these is proportional to the sending-end voltage, the other is proportional to the line drop, and the relay combines these so that the resultant torque is proportional to the receiving-end voltage. The former torque component is obtained by impressing on the relay-potential coil a voltage proportional to that at the sending end or regulator location. The torque component due to the line drop is obtained by means of a second upper-pole winding (see Figure 2). Current flowing in this winding from a current transformer in the line induces a flux, which reacts with the main-pole flux to produce a torque on the disk which opposes the main-relay torque.

If the relay is to perform correctly, the compensation torque must satisfy certain conditions which are a function of the electrical properties of the line, the magnitude of the load, and the power factor of the load. Consider a simple line, such as shown in Figure 3a. The vector diagram, Figure 3b, is drawn for a constant magnitude of load, I_L, and a varying load-power factor, θ. ϕ is the impedance angle of the line. This diagram shows clearly that the sending-end voltage, E_S, necessary to maintain constant receiving-end voltage, E_R, with varying power factor, has a maximum magnitude when δ, the angular

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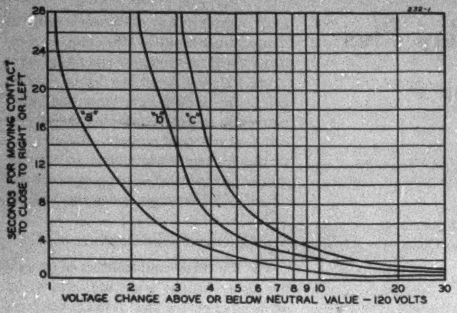

Figure 1. Voltage-regulating-relay time curves

(a). Right-hand contact set at 121 volts; left-hand contact set at 119 volts

(b). Right-hand contact set at 122 volts; left-hand contact set at 118 volts

(c). Right-hand contact set at 123 volts; left-hand contact set at 117 volts

shift between E_S and E_R is zero. This occurs when $\theta=\phi$. In order words, for any given load the relay must produce a maximum of compensation when the power factor of the load is equal to the impedance angle of the line.

What does this mean as far as the design of the relay is concerned? Referring to the general torque equation 1, it is seen that the compensator torque will be a maximum for any given load, when the angle between the compensator-winding flux and the potential-coil flux is 90 degrees. Therefore, a necessary condition for correct compensation is that when the load-power-factor angle equals the line-impedance angle, the compensator flux must be 90 degrees from the main-pole flux.

To see how this condition is translated into the actual structure of the relay, refer to the vector diagram, Figure 4.

Here E_1 is the voltage impressed across the relay terminals, and E_{1G} lags this somewhat due to the leakage drop in the potential coil. Φ_1, the main-pole flux lags E_{1G} by 90 degrees, and for maximum compensation torque Φ_C lags Φ_1 by 90 degrees. The compensator excitation which is proportional to the current in the compensator current transformer, leads ϕ_C by a small angle due to the eddy currents in the disk and the hysteresis loss in the upper-pole iron. Thus, the relay primary voltage, E_1, and the compensator current, i_c, will be very close to 180 degrees out of phase for maximum compensator torque. Actual measurements showed the angle to be 180 degrees ± 3 degrees.

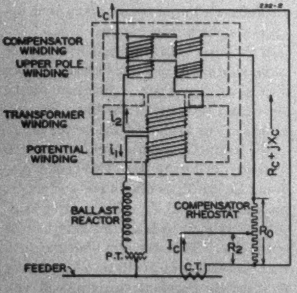

Figure 2. Schematic wiring diagram of voltage-regulating relay

As shown above, if the relay is to compensate correctly, maximum compensation torque must occur when E_S is in phase with E_R, and the load-power-factor angle is equal to the impedance angle of the line. Hence, in Figure 4, E_S is drawn at an angle $\theta=\phi$ ahead of I_L, or $180-\phi$ behind i_c. Then, E_1 must lag E_S by the impedance angle of the line. This means that a phase shifting network must be placed between E_S, the sending voltage, and E_1, the relay primary voltage. If the effective shift of this network is equal to ϕ, the relay will compensate correctly.

In appendix II is an analytical derivation which corroborates the conclusions reached above. The voltage equation for a simple line is set up and compared with the torque equation of the relay, and it is shown mathematically that the necessary and sufficient conditions for correct compensation are that the compensator-winding excitation be proportional to the magnitude of line drop and that the relay primary voltage lag behind the sending voltage by an angle equal to the impedance angle of the line.

This will be clearer if we consider for a moment the line-drop compensators almost universally used at present. These devices must also satisfy two conditions if correct compensation is to result. The compensator-reactance drop must be proportional to the line-reactance drop and the compensator-resistance drop must be proportional to the line-resistance drop. To get this proportionality for different lines, the compensator has an adjustable resistor and an adjustable reactor. In the new relay we fit the two line constants by having one adjustment for the impedance of the line in ohms, and another for the phase angle of the line impedance. In other words, we compensate for $Z/\underline{\phi}$, instead of $R+jX$. In both cases there must be two variables of adjustment to match two line constants.

The phase-angle adjustment in this new relay is provided by a phase-shifting network. Theoretically, this should be adjustable to vary the phase position of the output voltage, while keeping the magnitude of output voltage constant. But to obtain such results requires relatively complex networks. However, as will be shown later, a great simplification in design is possible.

Figure 2 shows the circuit for obtaining magnitude adjustment. This consists of a potentiometer which is connected to the line current transformer and the compensator winding so that as the slide is moved, the magnitude of current in the compensator varies while the phase angle of the current stays constant. This is easily seen to be true, for if I_c is the current in the transformer secondary, then the compensator current is given by

$$i_c=I_c\frac{R_2}{R_0+R_c+jX_c} \tag{2}$$

in which the various quantities are as indicated on Figure 2. The denominator of equation 2 is a constant, hence only the magnitude of i_c varies with R_2.

The rheostat may be calibrated in line ohms, or volts line drop. The latter method is actually used on the relay.

Settings

It is usually desirable that voltage-regulating relays be as inexpensive and simple in operation as possible, consistent with a minimum sacrifice in operating features. For this reason it is desirable to simplify the relay described in the pre-

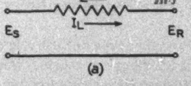

Figure 3a. Schematic single-phase feeder

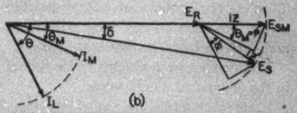

Figure 3b. Vector diagram of feeder

θ_M—Power factor for maximum line-drop compensation

I_M—Load current for maximum line-drop compensation ceding paragraphs, even though this might mean some limitations in operating performance.

The part of the relay most susceptible to simplification is the phase-shifting network of the line-drop compensator. As shown above, this network should provide adjustable voltage phase-angle shift without affecting the magnitude of voltage. Circuits to operate in this manner can be constructed with inductors, resistors, and capacitors. The finer the adjustments, the larger the number of elements required and the more difficult the calibration. Such networks were experimentally constructed and they operated satisfactorily.

The simplified network consisted of the single reactor used also as ballast impedance for temperature correction. Obviously, once the reactor is chosen, the phase-angle adjustment is fixed, and the relay is theoretically adapted for only one line. Nevertheless, it is possible to take a relay so constructed and apply it to a wide range of lines without excessive error in voltage regulation. Suppose the reactor has been designed so that when placed in series with the potential coil of the relay the angle between the impressed voltage and the voltage across the relay is 40 degrees. This means that when used on a 40-degree line, the relay operates correctly for any load and any power factor, if the compensator-voltage scale is set for the true magnitude of full-load line drop. Now, suppose we place this 40-degree relay on a line whose impedance angle is 70 degrees. The relay would no longer give correct voltage indications if the compensator-voltage scale is set for the true magnitude of full-load line drop. However, for any given power factor, a setting of the voltage scale can be found which makes the relay operate correctly for any load *at that power factor*. This setting in volts will obviously not be equal to the true line drop. We define this compensator setting, in volts, divided by the true line drop, in volts, as the "compensator-correction factor." In effect, an intentional error in the compensator-voltage setting is introduced to correct for the error in the relay-phase-angle setting.

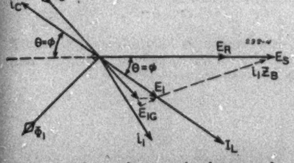

Figure 4. Vector diagram of voltage-regulating relay drawn for maximum compensation torque

Figure 5 is a family of curves to show how this factor varies as a function of load power factor when a 40-degree relay is used on various lines. Forty degrees was chosen because it is the impedance angle of the average low-capacity line.

The derivation of the correction factor curves is shown in appendix III. In order to verify these curves experimentally, apparatus was set up to simulate a line whose impedance angle could be varied, and a load whose power factor could be varied. Agreement between experimental and theoretical curves was very close.

A glance at the correction factor curves shows what errors may be expected in operating the relay on lines other than 40 degrees. Suppose we have a 60-degree line where the power factor varies from 80 to 90 per cent. The correction factor for 80 per cent is 0.91, for 90 per cent it is 0.85. The average setting would then be 0.88 and the variation plus or minus 0.03. Thus, if the line drop were 10 volts, the relay-compensator scale would be set at 0.88×10=8.8 volts and the maximum error plus or minus 0.3 volts. Obviously, the narrower the range of power factor, the smaller would be the relay-voltage errors. Also, the closer the line-impedance angle is to 40 degrees, the smaller is the error. As is the case with the compensators in use at present, calculated settings are only tentative, and final settings are usually chosen on the basis of field tests.

Conclusions

Due to the inverse time characteristic and integrating properties of the relay described above, circuits controlled by it are operated after a relatively short time delay when voltage conditions on the line require this. However, unnecessary operations are prevented when short-interval voltage surges occur. At the same time the relay is simplified to have only one adjustment for line-drop compensation without introducing excessive errors in indicated voltage.

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Appendix I. Symbols

E_S = sending-end voltage
E_R = receiving-end voltage
E_1 = voltage impressed on relay-potential coil
e_2 = voltage across N_2
T_c = compensator-torque component
T_S = sending-voltage-torque component
Φ_1 = flux due to potential-coil excitation
Φ_2 = induced upper-pole flux
Φ_c = flux due to compensator-winding excitation
i_1 = potential-coil current
i_2 = induced upper-pole current
i_c = compensator winding current
I_c = current-transformer secondary current
I_L = line current
N_1 = potential-coil turns
N_2 = turns of transformer winding on lower pole
N_3 = turns on each upper pole
N_c = turns per pole of compensator winding
$Z_2 = R_2 + jX_2$ = upper-pole impedance
Z_B = ballast impedance
$Z_c = R_c + jX_c$ = compensator impedance
R_3 = portion of compensator resistance across line current transformer
R_0 = total resistance of compensator
a = angle between Φ_1 and Φ_2
b = upper-pole impedance angle
θ = load-power-factor angle
ϕ = impedance angle of line
δ = angular shift between E_S and E_R
F = correction factor
β = angle between ϕ_1 and ϕ_c

Appendix II. Derivation for Minimum Temperature Error

By equation 1

$$T_S = K\Phi_1\Phi_2 \sin a$$

Referring to Figure 6

$$\sin a = \sin(90 - b) = \cos b$$

Therefore

$$T_S = K\Phi_1\Phi_2 \cos b$$

$$= K\Phi_1\Phi_2 \frac{R_2}{\sqrt{R_2^2 + X_2^2}}$$

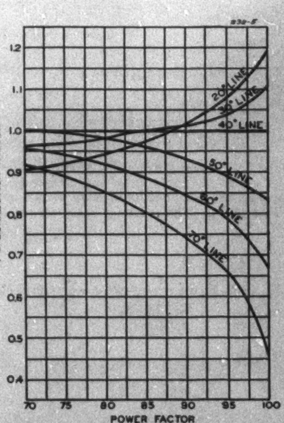

Figure 5. Correction-factor curves for 40-degree voltage-regulating relay. See appendix IV

Now

$$\Phi_2 = ci_2 = \frac{ce_2}{Z_2} = \frac{k}{\sqrt{R_2^2+X_2^2}}$$

where c and k are constants.
Or

$$T_S = \frac{kK\Phi_1 R_2}{R_2^2+X_2^2} = \frac{AR_2}{R_2^2+X_2^2}$$

Differentiate this expression with respect to R_2 to find the value of R_2 which results in no change in T_S for small changes in R_2.

$$\frac{dT_S}{dR_2} = 0 = \frac{A}{R_2^2+X_2^2} - \frac{2AR_2R_2}{(R_2^2+X_2^2)^2}$$

where A is a constant.
Solving

$$R_2 = X_2$$

This is the condition for minimum temperature error.

Appendix III

Derivation to prove that the relay performs correctly if:

(*a*) The compensator-winding excitation is proportional to the magnitude of line drop.

(*b*). Maximum compensator torque occurs when the line-impedance angle ϕ, equals the power-factor angle θ. That is, β, the angle between Φ_c and Φ_1 shall be 90 degrees when $\theta=\phi$.

1. The Voltage Equation for the Line

Refer to Figure 3b and take E_S as reference vector.

$$\mathbf{E}_R = \mathbf{E}_S - \mathbf{I}_L\mathbf{Z}$$

$$= E_S\underline{/0} - I_L\underline{/\delta-\theta}\cdot Z\underline{/\phi}$$

$$= E_S\underline{/0} - I_LZ\overline{/\theta-\delta-\phi}$$

Let

$$\theta-\delta-\phi=\gamma$$

Then

$$\mathbf{E}_R = E_S - I_LZ\cos\gamma + jI_LZ\sin\gamma$$

or

$$|E_R| = \sqrt{E_S^2 - 2I_LZE_S\cos\gamma + (I_LZ)^2} \qquad (3)$$

2. The Torque Equation of the Relay

The two components of relay torque are T_S and T_c. The resultant relay torque is T.

$$T = T_S - T_c$$

$$T_S \,\alpha\, \Phi_1\Phi_2 \sin a \,\alpha\, \frac{N_1^2}{N_2}\cdot i_1^2\cdot \sin a \,\alpha\, \frac{N_1^2}{N_2}\cdot \sin a\cdot E_S$$

Since

$$i_1 \,\alpha\, E_S$$

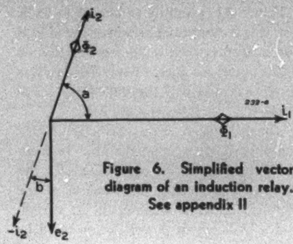

Figure 6. Simplified vector diagram of an induction relay. See appendix II

therefore

$$T_S = kE_S^2 \qquad (4)$$

Since $(N_1^2/N_2)\sin a$ is a constant

$$T_c \,\alpha\, \Phi_c\Phi_1\sin\beta$$

$$T_c = CI_cE_S\cos(\beta-90)$$

where C is a constant
Since $I_c\,\alpha\,\Phi_c$ and $\Phi_1\,\alpha\,E_S$, therefore

$$T = kE_S^2 - CI_cE_S\cos(\beta-90) \qquad (5)$$

The relay is initially calibrated with the compensator scale set for zero volts compensation, so that if E_R' is the scale reading of the relay, equation 4 applies and we have

$$|E_R'| = \sqrt{T/k}$$

Substituting for T from equation 5

$$|E_R'| = \sqrt{E_S^2 - KI_cE_S\cos(\beta-90)} \qquad (6)$$

Equations 6 and 3 are identical (if we neglect the term $(IZ)^2$ which is of the second order) when

(*a*). $KI_c = 2I_LZ$

(*b*). $(\beta-90)=\gamma=\theta-\delta-\phi$

That is, when $\theta=\phi$, and $\delta=0$; $\beta=90°$.

This is what we have set out to prove for (*a*) states that KI_c is proportional to the line drop, and (*b*) states that the angle between Φ_c and Φ_1 is 90 degrees when $\theta=\phi$.

Appendix IV. Derivation of Correction-Factor Curves

1. Assume the relay is adjusted to compensate for a line of impedance angle ϕ' and a voltage drop $(I_LZ)'$. The true line drop (I_LZ), and the true line angle is ϕ. Then, for a load power factor θ, the relay develops a torque in accordance with the voltage equation

$$E_S\overline{/\delta} = E_R\underline{/0} + (I_LZ)'\underline{/\phi'-\theta} \qquad (7)$$

E_R is taken as reference vector, and is the required receiving-end voltage.

2. The true line angle is ϕ, and the true line drop I_LZ. Then, since E_S is the head-end voltage of the line determined by the relay, the voltage equation of the line is given by

$$E_S\overline{/\delta} = E_{R_0}\underline{/\alpha} + I_LZ\underline{/\phi-\theta} \qquad (8)$$

Where E_{R_0} is the actual voltage at the end of the line.

In the equations below $(I_LZ)'$ is determined as a function of the line and relay constants so that $E_{R_0}=E_R=$ the required receiving end or load center voltage.

Equate (7) and (8)

$$E_R\underline{/0} + (I_LZ)'\underline{/\phi'-\theta} = E_R\underline{/\alpha} + (I_LZ)\underline{/\phi-\theta}$$

Solve for (I_LZ)

$$(I_LZ)'\underline{/\phi'-\theta} = E_R\underline{/\alpha} - E_R\underline{/0} + (I_LZ)\underline{/\phi-\theta}$$

$$(I_LZ)'\sin(\phi'-\theta) = E_R\sin\alpha + (I_LZ)\sin(\phi-\theta)$$

$$(I_LZ)'\cos(\phi'-\theta) = E_R\cos\alpha - E_R + (I_LZ)\cos(\phi-\theta)$$

Divide these equations to eliminate $(I_LZ)'$, simplify and solve for (I_LZ)

$$(I_LZ) = E_R\,\frac{\sin\alpha - [\tan(\phi'-\theta)\cdot(\cos\alpha-1)]}{\cos(\phi-\theta)\tan(\phi'-\theta) - \sin(\phi-\theta)}$$

Solve for $(I_LZ)'$

$$(I_LZ)\underline{/\phi-\theta} = E_R\underline{/0} + (I_LZ)'\underline{/\phi'-\theta} - E_R\underline{/\alpha}$$

$$(I_LZ)\sin(\phi-\theta) = (I_LZ)'\sin(\phi'-\theta) - E_R\sin\alpha$$

$$(I_LZ)\cos(\phi-\theta) = (I_LZ)'\cos(\phi'-\theta) - E_R\cos\alpha + E_R$$

Divide these equations to eliminate (I_LZ), simplify, and solve for $(I_LZ)'$

$$(I_LZ)' = \frac{E_R\cdot\tan(\phi-\theta)\cdot(\cos\alpha-1) - \sin\alpha}{\cos(\phi'-\theta)\tan(\phi-\theta) - \sin(\phi'-\theta)}$$

$$F = \frac{(I_LZ)'}{(I_LZ)} =$$

$$\frac{[\cos(\phi-\theta)\tan(\phi'-\theta) - \sin(\phi-\theta)]\cdot[\sin\alpha - \tan(\phi-\theta)(\cos\alpha-1)]}{[\cos(\phi'-\theta)\tan(\phi-\theta) - \sin(\phi'-\theta)]\cdot[\sin\alpha - \tan(\phi'-\theta)(\cos\alpha-1)]}$$

For line drops of the order of 10 per cent δ is very small and hence α must be even smaller.

Therefore

$$\sin\alpha = \alpha$$

$$\cos\alpha = 1$$

or

$$F = \frac{\cos(\phi-\theta)\tan(\phi'-\theta) - \sin(\phi-\theta)}{\cos(\phi'-\theta)\tan(\phi-\theta) - \sin(\phi'-\theta)}$$

The curves on Figure 5 are graphs of F versus θ for various line-impedance angles, ϕ. $\phi'=40$ degrees.

A Static Voltage Regulator Insensitive to Load Power Factor

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THE voltage regulator described in this paper is intended for those applications where the operating efficiency is of secondary importance to other requirements. Excluding the consideration of efficiency, the output voltage of an ideal voltage regulator should be of sinusoidal wave form and should remain constant with any variation in input voltage and frequency and any variation in load volt-amperes and power factor.

These are ideal requirements, and a regulator has not been designed which will fulfill them completely. Several types of static regulators have been available in recent years, but all of them have had one or more serious defects. The more serious of these defects are sensitivity to frequency and power-factor variations and slow speed of response. Since most voltage sources have good frequency regulation, the power-factor sensitivity has been the most prevalent defect in previous regulators. Because of this, a given regulator has had a limited flexibility in its application.

The voltage regulator described in this paper has the important distinction fundamentally of being independent of load power factor. It will meet all of the other requirements except frequency variations of an ideal regulator within limits of which can be summarized as follows:

The regulator can be designed to maintain an output voltage which will not vary more than ±2$^1/_2$ per cent for a simultaneous variation of 30 per cent in line voltage, 100 per cent in load, and any desired range of power factor. For fixed loads, the regulation can be held to less than ±0.5 per cent for 30 per cent variation in line voltage. The regulator will re-establish the load voltage within three cycles after a sudden change of 30 per cent in line voltage or 100 per cent in load is imposed on it. A variation of one-half cycle above or below the rated frequency will change the level of output voltage but will not materially affect the regulation of the new frequency. The wave form at or near full load will have a maximum harmonic content of about 6 per cent, and at open circuit its harmonic content reaches a maximum of 20 per cent. The efficiency of a one-kilovolt-ampere regulator or larger is 90 per cent or higher at full load.

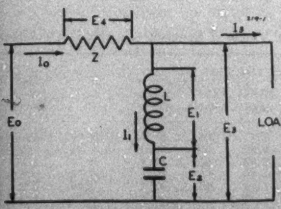

Figure 1. General circuit diagram for voltage regulator

Fundamental Circuit

The fundamental circuit employed in the regulator is simple and consists of a reactor and capacitor connected in series and shunted across the load as in Figure 1. The reactor has its individual characteristics, however, and these must be properly correlated with those of the capacitor. The reactor contains a special magnetic circuit[1] employing a bridge gap which will produce the volt-ampere curve E_1 in Figure 2. The capacitor is chosen so that its volt-ampere curve E_2 will be parallel to that of E_1. In an ideal circuit where L and C are pure inductance and capacitance, the voltage E_3 at the extreme terminals of the series circuit will be the numerical difference between the inductive and capacitive voltages. In Figure 2, therefore, the voltage E_3 can be represented by the vertical distance between the two volt-ampere curves, and this will remain constant between values of current represented by i_1 and i_1'. (In the following discussion capital letters are used to represent general values while lower-case letters represent minimum values and lower-case letters with primes [′] represent maximum values.) The permissible variation in series current is utilized in a series impedance Z in Figure 1 to compensate for changes in line voltage and load characteristics.

Assume, for example, an ideal circuit as in Figure 1 with the series impedance considered as pure resistance. Assume also that the circuit is operated at no load on a line voltage of some value which causes the minimum current of i_1 to flow through L and C. The vector diagram for this case is given in Figure 3 for which the following equations apply considering that $I_1=I_0$.

$$e_0^2=e_3^2+(i_1Z)^2 \tag{1}$$

$$(e_0')^2=e_3^2+(i'Z)^2 \tag{2}$$

Subtracting equation 1 from equation 2

$$(e_0')^2-e_0^2=(i_1'Z)^2-(i_1Z)^2 \tag{3}$$

Thus as the line voltage increases, I_1 increases to a value where the impedance drop E_4, (i_1Z), exactly compensates for the increase in line voltage. If the line voltage continues to increase, I_1 may exceed its limiting value of i_1' and the load voltage represented by the vertical distance between E_1 and E_2 in Figure 2 will decrease. This calls for a further increase in E_4 and a still higher value of I_1. Thus the load voltage drops very rapidly when the limits of I_1 are exceeded.

Now consider the same circuit operating on a constant line voltage and a variable resistance load. The vector diagram in Figure 4 shows the relation between I_1 and load current where it is important to note the minimum value of I_1 concurs with the maximum load current and that I_1 increases as the load decreases. The change in I_1 is dictated again by the amount of voltage drop in the series impedance Z necessary to compensate for the change in drop caused by the change in load current. In general the following vectorial equations apply:

$$E_0=I_0Z+E_3=E_4+E_3 \tag{4}$$

and

$$I_0=I_1+I_3 \tag{5}$$

The load current may have any value and phase angle with respect to E_3; and E_0 may fluctuate over any range, with the only restriction being that the limits of

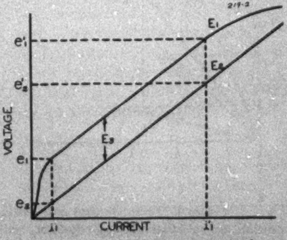

Figure 2. Volt-ampere curves of nonlinear reactor E₁ and associated capacitor E₂

Paper 42-19, recommended by the AIEE committee on industrial power applications, for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 6, 1941; made available for printing December 9, 1941.

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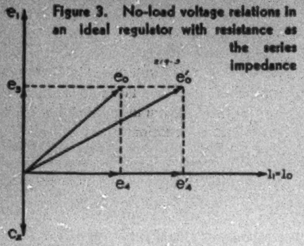

Figure 3. No-load voltage relations in an ideal regulator with resistance as the series impedance

I_1 are i_1 and i_1' as prescribed by Figure 2. If the load current becomes too high I_1 drops below the limiting value of i_1, and the voltage E_3 drops rapidly.

The characteristics of the series impedance Z materially affects the permissible range over which the various quantities are permitted to vary. A constant current type of series impedance shown in Figure 5 has several advantages. This circuit consists of a reactor and capacitor, connected in parallel, with volt-ampere relations similar to those of Figure 2. The line current is the difference between the capacitor and reactor currents at any value of their common voltage and can be represented by the horizontal distances between the two curves. It can be shown[1] that this circuit will maintain a constant current over a range of voltage from e_4 to e_4' which on Figure 2 would correspond to the voltages e_1 and e_1'. Thus another set of limits for correct operation must be added to those previously mentioned. The voltage drop E_4 across the parallel capacitor and reactor must remain within the limits of e_4 and e_4' over which there is a constant horizontal distance between the volt-ampere curves.

Analysis of the Practical Circuit

An accurate analysis of the regulator circuit must consider the actual power

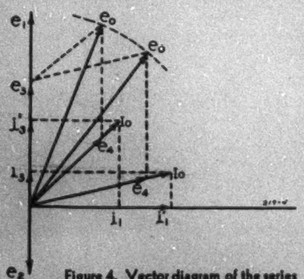

Figure 4. Vector diagram of the series portion of an ideal voltage regulator, with resistance as the series impedance, at constant line voltage and variable load

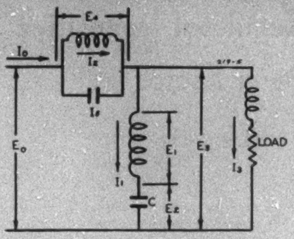

Figure 5. Complete circuit diagram of the preferred arrangement of the voltage regulator

factors of all of the reactors and capacitors in the circuit. A mathematical solution for the operating range can be established, but in practice, a graphical solution from a simplified vector diagram is quicker and accurate enough for design purposes. Hence a discussion of the vector diagram only will be attempted.

The complete diagram is given in Figure 6. In addition to equations 5 and 6, the following vectorial relations are needed.

$$I_0 = I_3 + I_1 \tag{6}$$

$$E_3 = E_1 + E_2 \tag{7}$$

The diagram shows all of the currents and voltages in their proper phase relations for a general load. The power factor of the reactors changes over their operating range which complicates an accurate vector analysis. However, experience has shown that average values of α and ϕ are accurate enough for the graphical calculations.

A simplified diagram may be constructed by using average values for α and ϕ. The vectors E_1, E_2, I_2, and I_6 may be omitted, and the voltage E_4 can be represented by a closing vector between E_3 and E_0. The diagram must include the range of the important variables if it is to be useful. Hence, it is convenient to represent the limits of E_4 by the arcs of two circles having the end of E_3 as their common center. The line-voltage vector will change in direction because I_1 has been taken as the reference axis, but its limits can be established by the arcs of two circles with their common centers at the origin. The locus of the constant line current is the arc of a circle, and the limits of I_1 can be indicated on the reference axis. A diagram incorporating these limits as well as variations in load is shown in Figure 7.

The actual design of a voltage regulator is based on a diagram similar to Figure 7. The variation in line voltage and the variation in load are prescribed by circumstances of the application. The limits of I_1 and E_4 are established by the parallelism of the volt-ampere curves and, under practical conditions, may cover ranges of 3 to 1 and 2 to 1 respectively. After a few adjustments of I_0, the values of I_1 and E_4 can be made to fall within their respective limits. Then, since the numerical values of line voltage and load current are known, all of the other currents and voltages can be scaled off of the diagram. A comparison between calculated and measured values of regulator currents and voltages is given in Table I.

Fundamentally, it is possible to design a regulator to operate any load over any range of power factor, leading or lagging; but, it is more economical to supply inductive power-factor correction for leading loads and limit the design of the

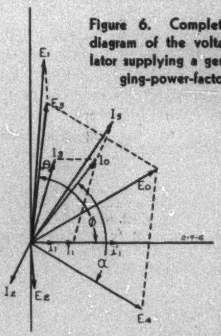

Figure 6. Complete vector diagram of the voltage regulator supplying a general lagging-power-factor load

Table I. Comparison of Calculated and Measured Values of Regulator Currents and Voltages

	I_0		E_4		I_1		E_3			
E_0	Calculated	Measured	Calculated	Measured	Calculated	Measured	Calculated	Measured	I_3	Load Power Factor
95	3.9	4.0	120	118	2.5	2.75	90	89.5	2.77	1.0
115	3.9	4.1	149	146	2.5	2.80	90	90.0	2.78	1.0
130	3.9	4.1	167	162	2.5	2.73	90	89.8	2.78	1.0
95	3.9	4.1	150	144	1.6	1.88	90	90.3	2.78	0.8
115	3.9	4.0	168	166	1.6	1.75	90	90.6	2.79	0.8
130	3.9	3.8	190	182	1.6	1.58	90	90.4	2.78	0.8
95	3.9	4.07	184	170	3.9	4.07	90	87	0	
115	3.9	3.7	204	196	3.9	3.7	90	90	0	
130	3.9	3.3	220	212	3.9	3.3	90	92	0	

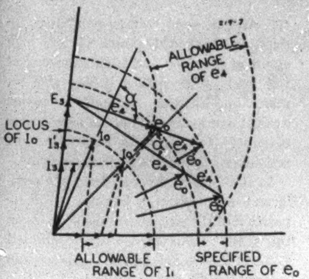

Figure 7. Design vector diagram

regulator to lagging loads. The reason for this is that an increase in power factor in a lagging direction reduces the value of current I_1, while an increase in a leading direction increases the value of I_1 beyond that required at no load. Thus, the capacity of every element of the circuit would have to be increased if the regulator were designed for both leading and lagging loads.

Characteristics

General

The equivalent characteristic of L and C in the series circuit is inductive, and the vector sum of its current I_1 and the load current must be constant. Therefore, I_1 will decrease for both an increase in load and an increase in lagging power factor.

The magnitudes of all quantities except E_4 in Figure 7 are independent of line voltage, but their phase angles with respect to the line voltage may change. Between full load and open circuit there is as much as a 90-degree phase shift between input and output voltage.

Stability

Stability of the regulator over its intended range of operation depends on the voltage relation between the series portion of the circuit and the line voltage, that is E_3 and E_0. If the vector E_3, in Figure 7, extends beyond the arc of minimum line voltage, the vector E_4 will intersect this arc at two points. Two values of E_4 will, therefore, satisfy the voltage triangle and the regulator may become unstable. Stable operation can be assured by establishing the value of E_3 somewhat less than the minimum value of line voltage.

Input Power Factor

The angle between E_0 and I_0 in Figures 6 and 7 is the power-factor angle on the line side of the regulator. This angle is leading for all values of load and line voltage. As the input current, I_0, is constant and independent of loading, the input volt-amperes will be proportional to the line voltage E_0. Therefore, as the load is decreased, the input power factor rapidly decreases until at no load the regulator becomes virtually a capacitor.

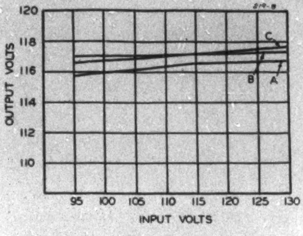

Figure 8. Voltage regulation curves

A—Open circuit
B—Full-load unity power factor
C—Full-load 0.8 power factor lagging

The input volt-amperes for a typical unit is approximately 150 per cent of the rated output at an average value of E_0.

Harmonic Content

The distortion of the output voltage wave shown in Figure 10a and b is caused primarily by a third harmonic. The analysis in Table II shows the comparison between input and output voltage for several harmonics. While the third harmonic is predominant, it reduces materially with load, whereas other harmonics are nearly independent of load.

Regulation

The curves in Figure 8 show the variation in load voltage for several conditions. This variation depends upon the accuracy of adjustment of the volt-ampere curves of the reactors. Since the curve E_1 is not a perfectly straight line between i_1 and i_1', the regulation is dependent also on the range over which I_1 is permitted to vary.

Table II. Harmonic Analysis of Regulator Voltage in Per Cent of Fundamental

		Regulator Output Voltage		
Order of Harmonic	Line Voltage	No Load	Full Load Unity Power Factor	Full Load 0.8 Power Factor
1	100	100	100	100
3	0.11	23.4	5.9	11.5
5	1.10	1.6	1.5	1.75
7	0.22	0.17	0.2	0.15
17	0.10	0.67	0.55	0.60
19	0.05	0.57	0.50	0.57

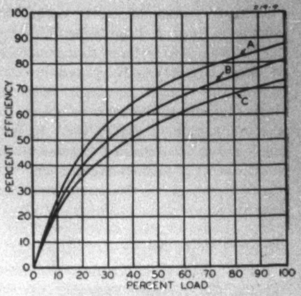

Figure 9. Efficiency curves

Unity-power-factor load
A—500 volt-ampere regulator
B—250 volt-ampere regulator
C—100 volt-ampere regulator

The effective value of output voltage is altered slightly by the change in wave form. It would change by three per cent from no load to full load on the basis of the harmonic content given in Table II. Thus considering the over-all regulation, the distortion does contribute an appreciable part.

The regulation can be improved by an adjustment in the volt-ampere curves of the parallel part of the circuit in Figure 5 so that I_0 decreases at the higher values of E_4.

Effects of Frequency

The effect of frequency on the operation of the regulator can be understood clearly by a reference to Figure 2. The volt-ampere curves are parallel for only one frequency, and, in general, the output voltage varies as the square of the frequency. In addition, the voltage regulation may be positive or negative depending on direction in which the frequency may change.

Efficiency

From the vector diagram in Figure 7 it can be seen that, as the load is decreased, the voltage increases across the various reactors and capacitors in the circuit. As a result, the losses are higher at no load than at full load, the no-load losses being approximately double the full-load losses. The general efficiency curves are shown in Figure 9.

Self-Protection

Mention has been made of the fact that as the load is increased above normal, the current I_1 in the series part of the regulator decreases. This is an important factor, because it guarantees that the regulator cannot be overloaded. When the load current begins to exceed its maximum limit, I_1 drops below i_1, Figure 2, and the load voltage drops rapidly. A short circuit can be placed directly across the output terminals with no damaging effect whatever, as shown by the oscillogram in Figure 10c. In fact the losses in the regulator decrease under short circuit conditions because only the parallel portion of the circuit remains effective.

SPEED OF RESPONSE

The oscillograms in Figure 10 show how rapidly the regulator restores normal voltage when a sudden change in line voltage or load is imposed on the circuit. A transient of about three cycles occurs during which the load voltage may rise or fall about ten per cent.

SIZE

The size and equivalent kilovolt-amperes of the regulator depend on the range over which it has to operate. For a typical unit which operates over a range of 30 per cent variation in line voltage, 100 per cent variation in load kilovolt-amperes and from unity power factor to 80 per cent lagging power factor the size and equivalent kilovolt-amperes will be approximately seven times that of a standard transformer of the same rating. If the range is reduced to 20 per cent line-voltage variation 50 per cent variation in load kilovolt-amperes and from unity to 90 per cent power-factor variation, the size and equivalent kilovolt-amperes will be reduced to approximately four to one.

Conclusions

There has been presented in this paper a new form of static voltage regulator which can be separated into two parts. The first is a series circuit consisting of a capacitor and reactor which, over a predetermined range of current passing through it, will maintain a constant output voltage regardless of load or power factor. The second consists of a circuit containing a capacitor and reactor in parallel which, over a specified range of voltage drop, will maintain a constant current regardless of the line voltage. Thus, the two elements in conjunction convert variable potential to constant current, then to constant potential.

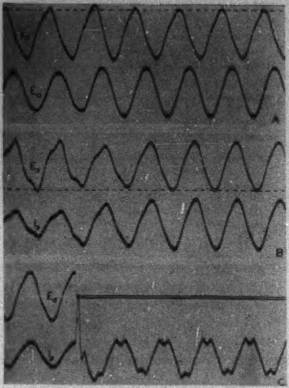

Figure 10. Voltage-regulator oscillogram

A—Transient in output voltage E_3 when line voltage E_0 is suddenly increased from 100 to 125 volts

B—Transient in output voltage E_3 when load current I_3 is suddenly increased from one-half load to full load

C—Transient in load current I_3 when a short circuit is placed on a regulator operating at full load

The regulator has many characteristics such as good voltage regulation, independent of load power factor, high speed of response and high efficiency at full load which makes it desirable for many applications. Its self protection against overloads makes it an ideal source of supply for filaments where the cold resistance approaches a short circuit.

In three-phase delta systems the regulator is unsatisfactory because the phase position of the output voltage depends on the load. However, in wye-connected systems three single-phase regulators can be used in each line to neutral.

The application of this regulator is necessarily limited by its size and cost. In general it will apply where some variation in load and power factor is expected and one or more of the following requirements are essential:

(*a*). High speed of response to eliminate the effect of transients.

(*b*). Very close regulation at any fixed load.

(*c*). Freedom from moving parts.

(*d*). Complete self-protection.

(*e*). Restricted overload capacity (maximum output current may be limited to as low as 125 per cent of rated current).

Sudden changes in line voltages of relatively short duration commonly occur on lines to which motors are connected, and these surges are passed through many types of voltage regulators. The three-cycle response speed of the regulator described above greatly attenuates these voltage fluctuations. This is an important factor in many applications for electronic devices and for testing purposes.

Reference

1. A STATIC CONSTANT-CURRENT CIRCUIT, C. M. Summers. AIEE TRANSACTIONS, volume 57, 1938 (November section), pages 636–9.

Multichannel Carrier-Current Facilities for a Power Line

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Synopsis: A carrier-current system providing a relatively large number of channels is in service on a high-voltage power line. The facilities provide:

1. Transmission of signals both ways for the opening of the circuit breakers at the remote end by the operation of relays at the near end.
2. Two-way telephone service between any extension telephone at the remote generating station and any telephone connecting to the main telephone switchboard in the central office building.
3. Two-way telemetering (station generation one way and system total generation the other way).
4. Transmission from the system operator's office to the station of automatically generated load-control signals used to increase or decrease automatically the station generation.
5. Two channels in each direction for future requirements.

This paper describes the methods employed to obtain these channels over a single power line, including the special precautions taken with the high-speed transferred trip signals to prevent faulty tripping of the line due to external disturbances which might generate carrier-current tripping pulses.

The Carrier-Current Circuits

A carrier-current system providing 15 channels on a single high-voltage transmission line is now in service. The transmission line connects directly to the transformers at each end, and circuit breakers are provided only on the low-voltage sides of the transformers. The carrier-current channels are used for high-speed transferred tripping, telemetering, load control, and telephone.

Normally just a few carrier-current channels are required on a power line. If more than about four channels are required, many problems arise because of the need of guard bands, the practical limit of two-carrier frequencies per coupling capacitor, complexities of wave traps for separating frequencies, and so on.

The 15 channels are obtained as follows: One carrier frequency is used for single-frequency duplex telephone service since standard equipment is available to perform this function. Two carrier frequencies are established and kept in operation at all times, one in each direction over the line, for the signaling carrier system. These carriers are modulated with the requisite number of audio tones required for the other services. At the receiving point, the audio tones are separated and used to operate relays or other suitable devices. Figure 1 shows a photograph of the equipment required at one terminal for the telephone, eight audio channels transmitted and six audio channels received.

The above plan requires three carrier frequencies. The tuning and line circuits for these frequencies as well as the power frequency are shown in Figure 2.

One will notice that the circuit for each frequency consists of two phase conductors and a grounded center tapped output transformer. The path of the carrier current may be over the two phase conductors, or one phase may be opened or grounded and a path will then exist between the other phase conductor and ground. In this way work may be performed on the line without interrupting the carrier-current circuits. In addition, double-frequency wave traps are interposed between the line grounding switches and the coupling capacitors so that the line may be grounded without disturbing the carrier-current path.

Each coupling capacitor couples two frequencies to each phase conductor. In order to segregate frequencies, separate line-tuning coils and wave traps are provided. At each terminal each carrier frequency is selected by two line-tuning coils, two wave traps, and a matching transformer.

Relay or Signaling Carrier System

A block diagram of the equipment required for the signaling carrier system is shown in Figure 3. The upper portion of the diagram shows the transmission system and the lower portion the receiving system. Signaling circuits operate the audio-tone generators numbered 1 to 8. The signal may either start or stop the oscillator, as may be required. However, it requires about three milliseconds for the audio frequency to either build up or decay, and, therefore, this limits the speed with which the tone can be controlled. In Figure 4 is shown the circuit of one of these audio oscillators and its amplifier.

The frequency stability of the oscillator must be reasonably good. The oscillator is tuned to the desired frequency by means of small fixed condensers and a coil with a movable powdered iron core. The core is adjusted by means of a screw to give a fine control of the frequency.

The outputs of the various oscillators are added by connecting the output transformers in series with the modulator. This must always be borne in mind, since opening a circuit at one point or even removing an amplifier tube will remove all the audio frequencies. In setting the frequencies of these tone generators, the frequencies must not be harmonics of one another nor should they be within 30 per cent of one another. A study of these

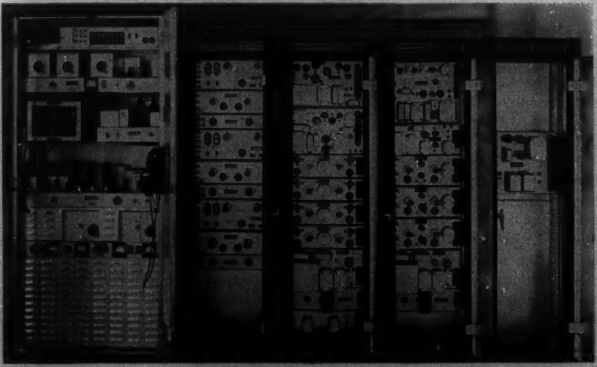

Figure 1. Carrier-current equipment set up for test

From left to right, the cabinets contain the following units: telephone, line tuning, relay transmitter, relay receiver, and transferred trip control

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Paper 42-13, recommended by the AIEE committees on automatic stations and power transmission and distribution, for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted April 14, 1941; made available for printing November 12, 1941.

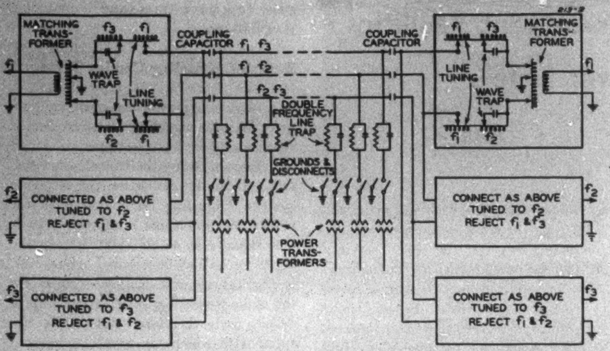

213-2
MATCHING TRANS-FORMER
WAVE TRAP
LINE TUNING
COUPLING CAPACITOR
DOUBLE FREQUENCY LINE TRAP
GROUNDS & DISCONNECTS
POWER TRANS-FORMERS
CONNECTED AS ABOVE TUNED TO f2 REJECT f1 & f3
CONNECTED AS ABOVE TUNED TO f3 REJECT f1 & f2
CONNECT AS ABOVE TUNED TO f2 REJECT f1 & f3
CONNECT AS ABOVE TUNED TO f3 REJECT f1 & f2
f1
f2
f3

Figure 2 (above). Line-tuning diagram

The circuits for one carrier frequency are shown and a block diagram is used for the other two

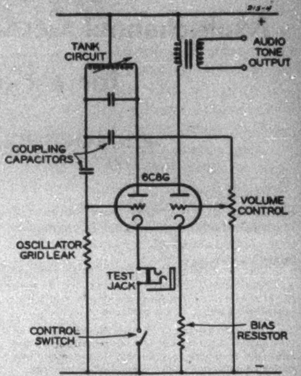

213-4
+
TANK CIRCUIT
AUDIO TONE OUTPUT
COUPLING CAPACITORS
6C8G
VOLUME CONTROL
OSCILLATOR GRID LEAK
TEST JACK
CONTROL SWITCH
BIAS RESISTOR
−

Figure 4. Simplified schematic diagram of audio oscillator and amplifier

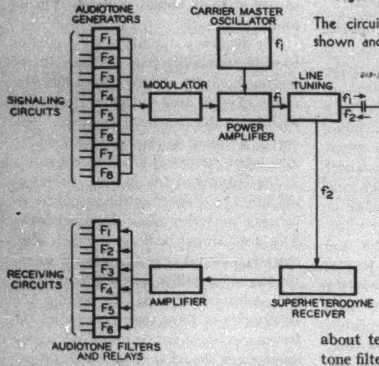

AUDIOTONE GENERATORS
CARRIER MASTER OSCILLATOR
213-3
SIGNALING CIRCUITS
F1
F2
F3
F4
F5
F6
F7
F8
MODULATOR
POWER AMPLIFIER
LINE TUNING
f1
f2
RECEIVING CIRCUITS
AMPLIFIER
SUPERHETERODYNE RECEIVER
AUDIOTONE FILTERS AND RELAYS

Figure 3 (left). Block diagram of relay- or signaling-carrier system

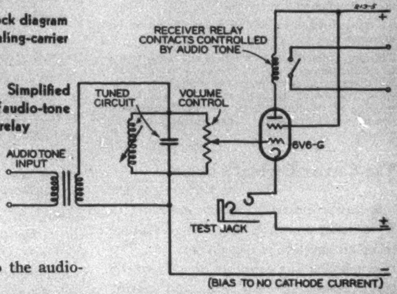

213-5
+
RECEIVER RELAY CONTACTS CONTROLLED BY AUDIO TONE
TUNED CIRCUIT
VOLUME CONTROL
AUDIO TONE INPUT
6V6-G
TEST JACK
±
−
(BIAS TO NO CATHODE CURRENT)

Figure 5 (right). Simplified schematic diagram of audio-tone receiver and relay

conditions shows that the frequency spectrum from 300 to 3,000 cycles will provide only about ten tones which will meet the above conditions. The equipment has been designed to accommodate a maximum of ten tones each way. The carrier frequency is supplied by a master oscillator which drives a power amplifier. This carrier frequency is always maintained so that a channel will be available for any audio tones which may be sent through. The power amplifier is plate modulated by the audio tones. From the power amplifier the carrier frequency is applied to the line tuning system shown in Figure 2.

At the far end of the line the carrier frequency is tuned by the proper line-tuning circuit and applied to a superheterodyne receiver. This receiver is equipped with both a squelch circuit, to prevent the reception of noise when no carrier frequency is present, and automatic volume control, to control the sensitivity to variations in carrier strengths. The output of the receiver is connected to an audio amplifier which provides about ten watts of power to the audio-tone filters.

The circuit of one of these audio filters is shown in Figure 5. The tuners are connected in series similar to the series connections in the transmitter. A parallel resonant circuit is tuned to each frequency which may be received by means of fixed capacitors and a coil with an adjustable powdered iron core. The voltage developed on the tuning circuit is applied to the grid of a detector tube which is biased to cut off when no signal is present. The arrival of signal of the frequency to which the circuit is tuned causes current to flow in the tube. This current normally passes through a relay which operates when the frequency is present.

The Transferred-Trip Circuit

One of the principal requirements of the signaling carrier system was to provide a high-speed transferred trip signal. The transformers on this line are not provided with circuit breakers on the line side. In case of a fault in a transformer, it is necessary to open the lower voltage breakers at both ends of the line in order to clear the transformer. The carrier is used to transmit the tripping signal to the far end of the line. Because of the large capacity of the transformers on this line, it was desirable that the time of transfer be as short as possible, and, at the same time, be completely reliable. In order to meet these conditions, it was decided to use three of the audio tones each way. These are arranged to provide the high-speed action desired together with automatic supervision and alarms.

The basic principle of the trip circuit is to have one of the audio frequencies, called a blocking frequency, always alive under normal conditions. This frequency upon reception, holds a relay contact open and prevents tripping. In order to trip, it is necessary to stop the blocking frequency and start another, called the tripping frequency. At the receiving point, a contact on the relay, associated with the tripping frequency, closes, and upon the closure of the contact associated with the blocking frequency, the trip circuit will be completed. Thus two things must oc-

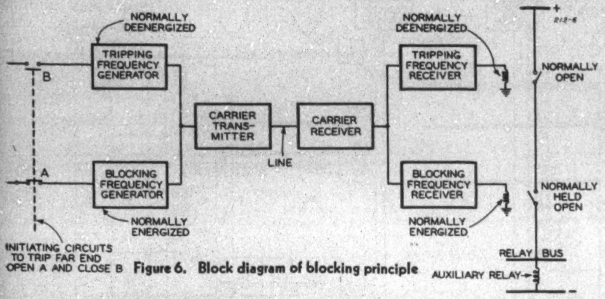

NORMALLY DEENERGIZED
TRIPPING FREQUENCY GENERATOR
B
A
CARRIER TRANS-MITTER
LINE
CARRIER RECEIVER
BLOCKING FREQUENCY GENERATOR
NORMALLY ENERGIZED
NORMALLY DEENERGIZED
TRIPPING FREQUENCY RECEIVER
BLOCKING FREQUENCY RECEIVER
NORMALLY ENERGIZED
+
NORMALLY OPEN
NORMALLY HELD OPEN
RELAY BUS
AUXILIARY RELAY
−
INITIATING CIRCUITS TO TRIP FAR END OPEN A AND CLOSE B

Figure 6. Block diagram of blocking principle

cur to cause a tripping action: First, the blocking frequency must be removed, and second, the tripping frequency must be started. As long as the blocking frequency is present, no tripping can occur even though the tripping-frequency relay might close from time to time, due to static, noise, and so on. Figure 6 shows this scheme in block diagram form.

The existence of the blocking frequency makes possible an interlocking supervising system. A block diagram of the circuit arrangement at the two terminals is shown in Figure 7. Frequencies *F*1 and *F*2 form one of the blocking loops. When the initiating switch 1 is in the normal position, it energizes the blocking oscillator *F*1. The blocking frequency *F*1 is transmitted and received; contacts 11 and 12 close and contact 13 opens. With the initiating switch 2 in the normal position, the closing of contact 11 causes the blocking oscillator to start, and the frequency *F*2 is in turn transmitted. At the other end of the line it is received. This causes contacts 21 and 22 to close and contact 23 to open. The closure of contact 21 lights a pilot lamp which shows that this blocking loop is complete and that the carrier equipment is normal, since any failure would result in the loss of the blocking frequency and the consequent extinction of pilot lamp.

A second blocking loop is provided by frequencies *F*3 and *F*4 in exactly the same way as the first blocking loop. This second loop causes contacts 31 and 32 to close and 33 to open at the remote terminal, and 41 and 42 to close and 43 to open at the supervising terminal. The condition of this blocking loop is shown by pilot lamp *B*.

These have been called blocking loops because they prevent tripping. This action occurs because contacts 13 and 33 are open at one end and, therefore, no feed is provided for the auxiliary relays; contacts 23 and 43 at the supervising end are open, also preventing tripping of this end.

The operation of either initiating switch 1 or 2, will trip the opposite end of the line. Operation of switch 1 removes the feed to the blocking oscillators (*F*1 and *F*3) and puts feed on the tripping oscillator (*F*5). At the far end of the line contacts 13 and 33 close due to the loss of the blocking frequencies, and contact 53 closes because the tripping-frequency receiver is energized. This provides feed to the auxiliary relay and trip circuits. Likewise operation of switch 2 will complete the circuit through contacts 23, 43, and 63, for tripping in the opposite direction.

Supervision of the carrier is obtained by other contacts on the relays. When a blocking loop is closed the push button receives feed through the corresponding contact 22 or 42. This push button tests the tripping frequency by means of a third loop. The test can only be made if one of the blocking loops is complete. Since there is no way of knowing whether the (*F*5) tripping-frequency oscillator or receiver, or the other (*F*6) tripping-frequency oscillator or receiver is in working condition, this test is necessary. Operation of the test push button closes contact 51 which in turn starts the tripping oscillator (*F*6) and closes the contact 61, indicated by the lighting of pilot lamp *C*. Pilot lamp *C* will light only when all the elements of the trip circuit are in operating condition. By connecting an alarm relay as shown in Figure 7, an alarm will be given whenever both blocking loops are lost. This occurs when something has become defective in the carrier circuit. To further aid in preventing trouble, due to the possible loss of carrier blocking coincident with the reception of undesired signals, the tripping-frequency receiver is set about one half as sensitive as the blocking-frequency receivers.

Initiating Circuits

In Figure 7 the initiating circuits are indicated as switches 1 and 2. In actual practice no switches are used for this purpose. The blocking frequencies are removed and the tripping frequency started directly from the closure of the primary relay contacts. Figure 8 shows schematically the essential features for transmission and reception, keeping the notation of Figure 7 as much as possible.

Two separate sets of contacts are provided on the primary relays, for two separate auxiliary relays. The carrier trip circuit must also feed from the same circuits as the auxiliary relays. Tubes *V*1 and *V*2 are provided as insulation between the two relay busses. Since only one carrier trip circuit is provided, the two relay busses must be paralleled, and yet back feeds from one bus to the other must be prevented. Tubes *V*1 and *V*2

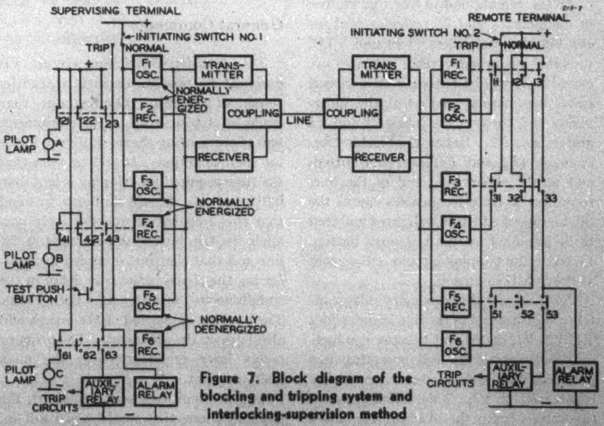

SUPERVISING TERMINAL
INITIATING SWITCH NO. 1
TRIP
NORMAL
+
F1 OSC.
F2 REC.
TRANS-MITTER
NORMALLY ENER-GIZED
COUPLING
LINE
RECEIVER
21
22
23
PILOT LAMP A
F3 OSC.
F4 REC.
NORMALLY ENERGIZED
41
42
43
PILOT LAMP B
TEST PUSH BUTTON
F5 OSC.
F6 REC.
NORMALLY DEENERGIZED
61
62
63
PILOT LAMP C
TRIP CIRCUITS
AUXIL-IARY RELAY
ALARM RELAY
−
REMOTE TERMINAL
INITIATING SWITCH NO. 2
TRIP
NORMAL
+
TRANS MITTER
COUPLING
RECEIVER
F1 REC.
F2 OSC.
11
12
13
F3 REC.
F4 OSC.
31
32
33
F5 REC.
F6 OSC.
51
52
53
TRIP CIRCUITS
AUXIL-IARY RELAY
ALARM RELAY
−

Figure 7. Block diagram of the blocking and tripping system and interlocking-supervision method

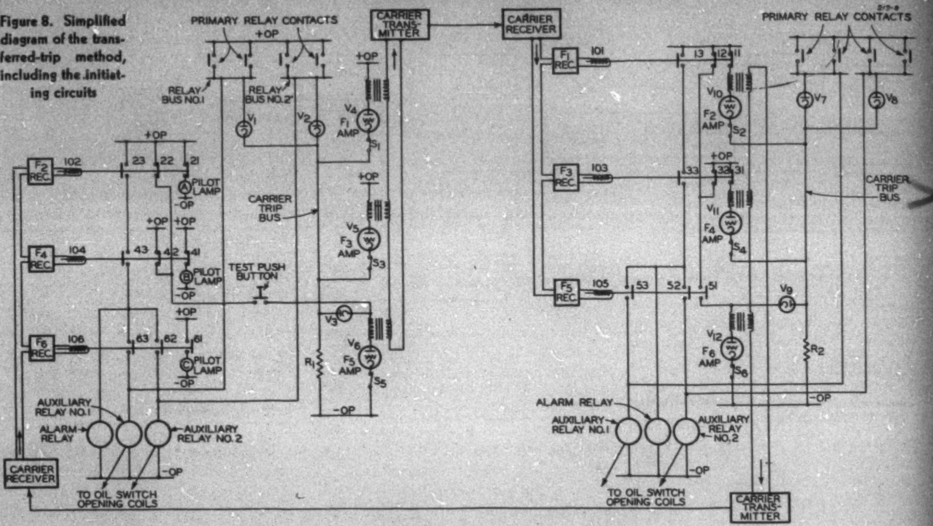


Figure 8. Simplified diagram of the transferred-trip method, including the initiating circuits

make this possible since they carry current in only one direction.

When the primary relay contacts are open, tubes *V*1 and *V*2 are nonconducting. Tubes *V*4 and *V*5 are amplifiers in the blocking loops. Under normal conditions they will have their starting switches closed and be in a condition to amplify. Their cathode current flows through resistor *R*1 and causes a voltage drop of about 15 volts on this resistor. This voltage is so small that the current through *V*3 and *V*6 is insufficient to amplify the tripping frequencies. The blocking frequencies (*F*1 and *F*3) are transmitted, and the tripping frequency (*F*5) is not.

At the remote end of the line closure of contacts 11 and 31 provides feed for the blocking amplifiers (*V*10 and *V*11), provided no primary relay contacts are closed. It will be noticed that a circuit exists here similar to that at the supervising end through the tubes *V*7 and *V*8 and resistor *R*2. Hence, the blocking frequencies (*F*2 and *F*4) are transmitted, and at the supervising end of the line, reception of these frequencies causes the pilot lamps *A* and *B* to be lighted and feed to be provided for the test push button. Likewise the tripping circuits are opened as previously discussed.

Upon closure of the primary relay contacts at either end of the line, either tubes *V*1, *V*2, *V*7, or *V*8 will become conducting, and practically full operating bus potential will be developed across either resistor *R*1 or *R*2. This action removes the voltage from the blocking amplifiers and causes either tube *V*3 or *V*9 to become conducting and provide feed for the tripping-frequency amplifier. This, of course, closes the trip circuit at the other end of the line as discussed previously. Tubes *V*3 and *V*9 are provided so that when the test push button is closed, the tripping amplifier can be energized without removing the voltage to the blocking amplifiers. Whenever the test push button is closed all six audio frequencies are present on the line. The alarm relays are also provided as described previously. The pilot lights and test push button are extended to the control room so that the condition of the carrier equipment can always be observed by the operators.

General Comments

This transferred tripping circuit was designed to provide as high a transfer speed as possible. Oscillograms were made on a test setup of the equipment, two of these being shown in Figure 9, one for each direction. It will be seen that the time required for tripping is less than 0.015 second. It will be borne in mind that this transfer circuit effectively parallels the trip busses at both ends of the line and that the time occupied in transferring the signal is the only delay in the simultaneous opening at both ends. The opening of one end of the line should always open the other end. The primary relays have preference over the push button circuit.

As the circuit has been explained, the tripping connection would seal itself in. To prevent this, back contacts were placed in the auxiliary relays at one end which would break the circuit as soon as these auxiliaries have operated.

Telemetering and Load Control

An impulse method of telemetering is used over the carrier-current circuit. The starting of the audio frequency used for telemetering indicates one impulse, and the stopping indicates a second impulse. In this way, the control of the audio oscillator is very simple. Load control signals of the time-duration type are also transmitted. These are based on the premise that the more the load is to be changed, the longer the pulse lasts. The starting and stopping of the audio frequencies, associated with the load control transmission, is rapid enough to cause inconsequential change in this timing.

Experiences With Equipment

As will be noted from Figure 2, the line-tuning system is quite complex, and it was considered too difficult to check the frequency response of the line using the actual equipment. A simple method was used for this measurement. Three resistors arranged in a star and grounded at the mid-point were connected at each end of the line. Drain coils were also connected across each resistor. A vari-

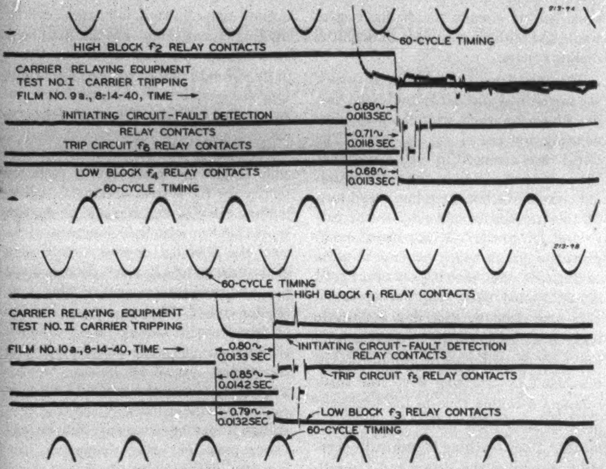


Figure 9 (above). Oscillograms showing time required for transference

The oscillograms were made during tests on the equipment. Test I is one direction and test II the opposite direction

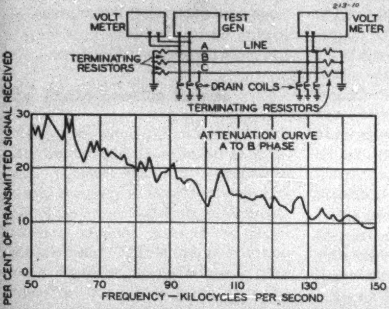


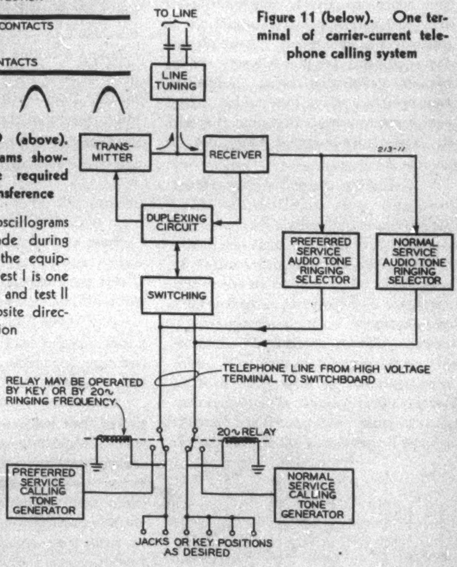
Figure 10 (above left). Circuit diagram of test method and curve of attenuation for the frequency range of 50 to 150 kilocycles

able frequency generator was connected to the phase wires, a pair at a time, and the transmission over the frequency band from 50 kilocycles to 150 kilocycles was obtained. Readings were made by vacuum-tube voltmeters at the sending end and the receiving end. Figure 10 shows the circuit used and the curve of response from *A* to *B* phases. For actual use, the received voltage should be doubled, since the terminating resistors are not present.

It was felt that noise would be the worse possible source of trouble so far as the transferred trip circuit was concerned, particularly noise from lightning. The blocking system was designed to prevent harm from this type of noise, but a defect was present at first. Whenever a noise pulse was received it overloaded the receiver and wiped off the modulation. It also gave a large pulse of negative voltage to the automatic volume-control circuit. When the noise pulse disappeared the automatic volume control was biased too far negative and the carrier signal was not received. As a result the blocking relays dropped out. In order to correct this the time constant of the automatic volume-control circuit was shortened to about $^1/_{60}$ of a second and with this arrangement the blocking relays are not affected when a noise is received. In actual practice these noise pulses are normally due to lightning discharges in immediate vicinity of either terminal. A noise pulse apparently does not affect the tripping-frequency relay.

So far in the operation of the equipment little trouble has been experienced in maintaining the audio signals. In the original setup of the frequencies it was necessary to separate the audio tones by at least 30 per cent and also to see that no frequencies were on the harmonics of lower frequencies. This rather limits the number of audio frequencies available in the band from 300 cycles to 3,000 cycles over which the equipment is designed to operate but there is sufficient space for the ten tones, the maximum number desired on this system. It has been found necessary to keep the percentage modulation of any individual frequency low to prevent interaction between any of the various audio channels. Even so, there is a slight interaction which thus far has not been serious. The signal levels in the modulator and amplifier at both transmitter and receiver must always be kept low to prevent the introduction of harmonics. It is not difficult however, to find working levels which are stable.

Figure 11 (below). One terminal of carrier-current telephone calling system


TO LINE
LINE TUNING
TRANS-MITTER
RECEIVER
213-11
DUPLEXING CIRCUIT
PREFERRED SERVICE AUDIO TONE RINGING SELECTOR
NORMAL SERVICE AUDIO TONE RINGING SELECTOR
SWITCHING
TELEPHONE LINE FROM HIGH VOLTAGE TERMINAL TO SWITCHBOARD
RELAY MAY BE OPERATED BY KEY OR BY 20~ RINGING FREQUENCY
20~ RELAY
PREFERRED SERVICE CALLING TONE GENERATOR
NORMAL SERVICE CALLING TONE GENERATOR
JACKS OR KEY POSITIONS AS DESIRED

The Carrier-Current Telephone System

The problems which developed in the carrier telephone system were mainly due to long extensions and the type of preferred service desired by the system operator. At one end of the line, the problem was simple because the carrier set was near the telephone switchboard. At the other end, however, about eight miles of leased line were required to connect the telephone switchboard and the system operator's office to the carrier equipment.

This distance was too great for the dialing equipment provided in the carrier sets and an additional calling system was devised. Furthermore, the duplexing equipment had to operate from extensions varying greatly in length. It was found that the variation between extensions was more than 20 decibels.

A block diagram of the calling system which was finally adopted is shown in Figure 11. A single carrier frequency is used for the telephone work. The transmitter and receiver are controlled by voice-operated duplexing relays. The manufacturer supplied standard equipment for this purpose, but the control circuit was changed to operate from the automatic gain control to provide compensation for the great variation in voice level due to differences in extensions. Also built into the equipment are line selectors, dialing circuits, and ringing relays. Additional tone-operated relays were added to this equipment to operate in conjunction with it. These tone-operated relays use the same ringing circuits the dialing system would use but without going through the dialing system.

The telephone switchboard, to which the carrier is connected by the leased line, has fifteen positions with the possibility of using any one of 225-cord sets. The circuit was arranged in such a way that the placing of a plug in one of the terminal jacks of the telephone line would turn on the transmitter and prepare the carrier set for service. It was necessary to provide a circuit which would send out a calling tone when the ring key of the cord set associated with plug was operated. This was done by inserting a 20-cycle relay in the jack circuit to operate from the normal switchboard ringing signal controlled by the ring key; the relay connects a calling tone to the telephone line and from there to the carrier transmitter. Upon reaching the normally energized receiver at the distant end, the calling tone operates a relay tuned to its frequency and it, in turn, indicates a call by lighting a call lamp on the telephone switchboard. As soon as the ring key is in its normal talking position, the calling tone stops, and when the called line answers, the equipment is ready for service.

The calling-tone system makes it possible to provide instantaneous preferred service for the system operator. A line-holding relay at the near end of the line is arranged to seize the equipment whenever the operator desires. Operation of the calling-tone key will then send out the preferred-service calling tone. At the far end of the line, reception of the preferred-service calling tone will break the normal telephone circuit and ring the system operator's phone. In this way the system operator can always gain control of the carrier telephone, even though it may be in use for ordinary telephone purposes. Busy lamps are provided to indicate when the line is in use to prevent unnecessary interruptions.

Relays and Breakers for High-Speed Single-Pole Tripping and Reclosing

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THE advantages of fast reclosing of transmission-line circuit breakers have been realized for a number of years. This experience has been gained on the basis of three-pole reclosures. One step beyond three-pole tripping and reclosure is single-pole operation. It is arranged so that on single phase-to-ground faults only the faulty phase wire is disconnected at each end of the line and then immediately reclosed. This allows synchronizing current to flow over the two sound-phase conductors during the time the faulted phase wire is out of service. With single-pole tripping slower speed reclosing, as compared to three-pole operation, can be utilized with a definite gain in the stability limit.[1]

The Phase-Selector Relays

Single-pole tripping from a relay standpoint has brought up a number of problems. At first thought it would appear to be only necessary to separate the trip circuits of the conventional relay equipments into three separate paths to the single-pole trip coils. This is true to a certain extent, depending upon the type of relay protection being considered. For instance, in the case of the simple overcurrent relay where phase relays will also operate for ground faults, it would be only necessary to arrange each phase relay to trip its respective single-pole breaker. However, it is the general practice at present to employ a separate ground relay operating on residual current and voltage or line residual current and power-bank neutral current in order to obtain adequate sensitivity to ground faults. The chief problem, therefore, involved in single-pole tripping is to find an effective way of indicating to the conventional residual-type ground relay which phase conductor is supplying the ground current. One way of providing this indication would be to employ under voltage relays energized from line-to-ground voltage. A fault to ground on one-phase conductor would drop out the corresponding voltage relay whose back contact in series with the conventional directional ground relay would trip its associated single-pole breaker. Another possibility would be the use of voltage-restrained overcurrent relays operating on line current and line-to-ground voltage. In general, neither of the above schemes is considered satisfactory on the basis that adequate sensitivity is not provided. It is quite possible that a high-resistance ground fault will not reduce the voltage sufficiently to provide a safe margin of discrimination.

The ideal approach to the problem would be to find a method of phase selection which can be made just as sensitive as the conventional directional ground relay and one which is, furthermore, totally independent of all normal conditions and dependent solely on fault conditions.

The method described below utilizing the phase shift of one sequence component with respect to another sequence component meets the above requirements.

Figure 1 shows the phase shift among the three sequence components for single phase-to-ground faults on different phases. It will be noticed that the zero-sequence component rotates around a given phase of the negative-sequence system in 120 degree steps. For instance, on a phase-*A*-to-ground fault the zero-sequence current is equal to and in phase with phase *A* of the negative sequence. This is, of course, on the basis of the total fault current. For a fault on phase *B* the zero-sequence component is in phase with the *B*-phase negative sequence, and, similarly, for a *C*-phase-to-ground fault the zero sequence is in phase with the *C* phase component of the negative sequence. Therefore, a selector element utilizing zero- and negative-sequence current will act in one direction for phase-*A*-to-ground fault but will act in the opposite direction for ground faults on *B* and *C* phases. Thus, if a directional element with watt-element characteristics is supplied with the zero-sequence current in one coil and the phase-*A* component of the negative-sequence current in the other coil, then, on a phase-*A*-to-ground fault this element will have maximum torque in the contact-closing direction. For a fault on phase *B* the zero-sequence current leads phase *A* of the negative sequence 120 degrees, and for a fault on phase *C* the zero-sequence current lags phase *A* of the negative sequence by 120 degrees. The element will, therefore, have torque in the contact-opening direction for phase-*B* and phase-*C* ground faults. If another similar relay element is used with its corresponding coil energized by the zero-sequence current, but its other coil energized by the phase-*B* component of the negative sequence instead of the phase-*A*, then, for faults on *B* phase it will be operating at maximum torque and will close its contacts but will have reversed torque for ground faults on phases *A* and *C*. Likewise, a third element energized by zero-sequence current and the phase-*C* component of the negative-sequence current will close its contacts for phase-*C*-to-ground faults, but open its contacts for faults on the other two phases. The schematic connections for this relay are shown in Figure 2.

The phase-selector relay consists of three elements similar to the conventional directional element. The polarizing coils of all three elements are energized by the zero-sequence current. The other coil on each element is energized by a component of the negative-sequence current obtained from the three-phase negative-sequence filter. This filter is so connected that the phase-*A* component of the negative-sequence system is supplied to the phase-*A* element, the *B* component supplied to the *B* element and the *C* component supplied

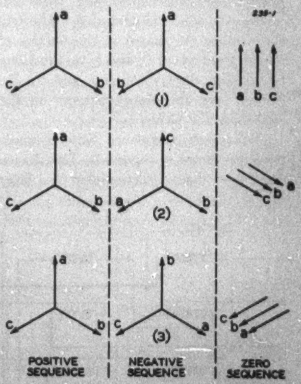

Figure 1. Current vectors for single-phase-to-ground faults on different phases

1. Phase-*A*-to-ground fault
2. Phase-*B*-to-ground fault
3. Phase-*C*-to-ground fault

Paper 42-35, recommended by the AIEE committees on power transmission and distribution and protective devices, for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 27, 1941; made available for printing December 11, 1941.

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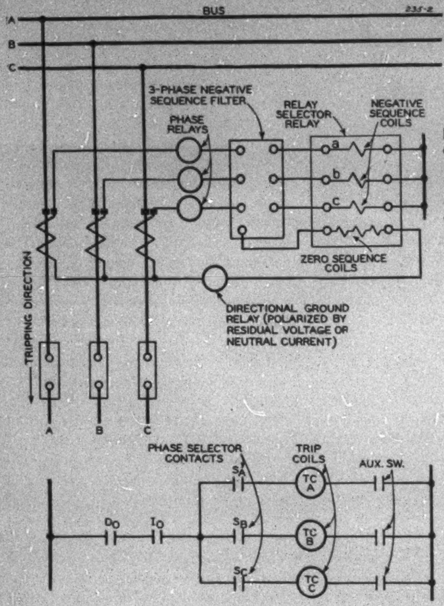
BUS
235-2
A
B
C
3-PHASE NEGATIVE SEQUENCE FILTER
PHASE RELAYS
RELAY SELECTOR RELAY
NEGATIVE SEQUENCE COILS
a
b
c
ZERO SEQUENCE COILS
DIRECTIONAL GROUND RELAY (POLARIZED BY RESIDUAL VOLTAGE OR NEUTRAL CURRENT)
TRIPPING DIRECTION
A
B
C
PHASE SELECTOR CONTACTS
TRIP COILS
AUX. SW.
D0
I0
SA
SB
SC
TC A
TC B
TC C

Figure 2 (above left). Schematic diagram of connections of phase-selector relay

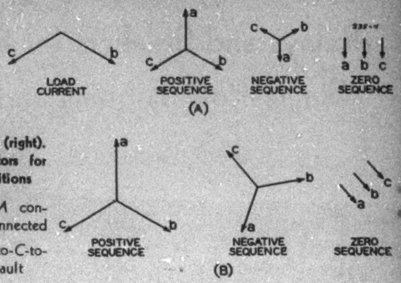
235-4
LOAD CURRENT
POSITIVE SEQUENCE
NEGATIVE SEQUENCE
ZERO SEQUENCE
(A)
a
b
c
POSITIVE SEQUENCE
NEGATIVE SEQUENCE
ZERO SEQUENCE
(B)

Figure 4 (right). Current vectors for two conditions

A—Phase - A conductor disconnected

B—Phase-B-to-C-to-ground fault

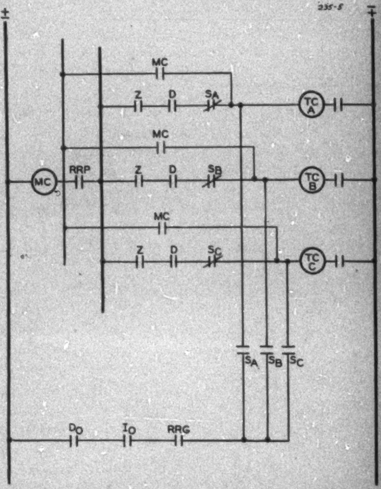
±
∓
235-5
MC
Z
D
SA
SB
SC
TC A
TC B
TC C
RRP
D0
I0
RRG

Figure 5 (right). Simplified schematic diagram of trip circuits

to the *C* element. The connections (Figure 2) show the inclusion of phase relays which may be of the conventional type and also a conventional directional residual-type ground relay. This relay is necessary to provide directional ground protection as the selector relay, while employing elements similar to the directional element, does not indicate the direction of power flow but merely the particular phase wire which is grounded. The trip connections are shown at the bottom of the diagram where D_o and I_o are the directional ground-relay contacts and S_A, S_B, and S_C are the make contacts on the phase-selector elements.

The negative-sequence filter connections are shown in Figure 3. This device consists of three filters similar to a filter which has been described previously and which has been in use for a number of years.[2-3] Three of these standard filter units are connected to form a three-phase unit so that it is entirely unresponsive to zero-sequence currents or positive-sequence currents, and the output consists of all three phases of the negative-sequence system.

Other pairs of sequences besides the negative- and zero-sequence currents may be used to produce a phase-selector action. For instance, zero-sequence current and positive-sequence voltage is a feasible combination, also, zero-sequence voltage and positive-sequence voltage. However, it is considered that the zero- and negative-sequence current combination described above is more desirable, since all voltage connections are avoided and the selector elements operate on quantities which appear only during fault conditions, and, for this reason, its sensitivity can be very easily made to match the sensitivity of the conventional directional residual-type ground relay.

An essential feature of any phase-selection scheme is that after a faulted phase has been selected for tripping, the phase-selector scheme should not operate, under the conditions obtaining while one phase wire is disconnected, to disconnect the unfaulted phases. It can be shown that the above selector scheme gives the same tripping indication after the faulted phase has been tripped out. With one phase wire disconnected, there will be load current

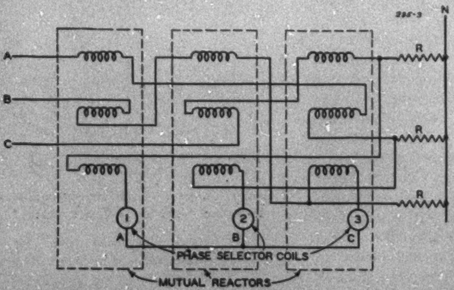
235-3
N
A
B
C
R
R
R
1
2
3
A
B
C
PHASE SELECTOR COILS
MUTUAL REACTORS

Figure 3. Schematic diagram of connections of three-phase negative-sequence filter

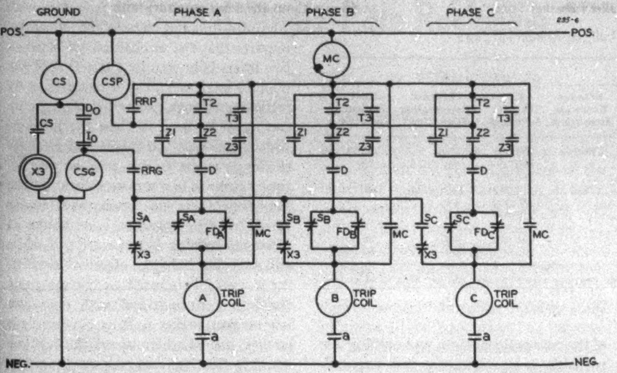

Figure 6. Complete schematic diagram of trip circuits for carrier-current single-pole relaying

on the other two phases, in general, equal and 120 degrees out of phase upon the assumption that each end of the protected line is fed from a grounded source.

Figure 4A shows this condition for the case where phase *A* has been tripped. It is seen that the zero-sequence and the negative-sequence currents are in phase on phase-*A* selector element. The phase selector, therefore, gives the same indication during the time a phase wire is out of service, thus preventing inadvertent tripping of the unfaulted phases. Figure 4B shows the vectors for a two-phase-to-ground fault on phases *B* and *C* and, it will be noted that this is a very similar condition to that obtaining when one phase wire is disconnected, in that the zero- and the negative-sequence currents are essentially in phase. On two phase-to-ground faults, therefore, the phase-selector relay selects the unfaulted phase.

The grounded phase-selector scheme can be applied in any system of phase and ground relays, but a description is given here of its use in a carrier-current step-type distance-relay scheme, as installed on the Public Service Company of Indiana system. In this application the following features were incorporated:

1. All single-phase-to-ground faults to be tripped single pole and reclosed immediately.
2. If fault still exists, then all three poles to be tripped.
3. On phase-to-phase faults, two phase-to-ground and three-phase faults all poles to be tripped and not reclosed.

It is interesting to examine how the simple phase-selector relay as described above is incorporated in a complete distance-type carrier-current system.

The schematic connections in Figure 5 have been simplified in order to show this incorporation as clearly as possible. The ground-relay trip circuit is made through the directional contact, D_o, overcurrent contact, I_o, the carrier-receiver relay contact, *RRG*, and one of the phase-selector contacts, S_A, S_B, or S_C. The phase-relay trip circuit is composed of the *RRP* carrier-receiver contact, the *Z* or distance-element contact and the directional-element contact, *D*. The purpose of the S_A, S_B, and S_C, back contacts in series with the respective phase-relay contacts is to prevent three-pole tripping in case a phase relay responds to a close-in phase-to-ground fault. Distance relays for phase protection will, in general, have a tendency to operate on heavy close-in ground faults and since it is arranged that all three poles are tripped when a phase relay operates, the single-pole tripping feature would not be obtained in the case where a phase relay operates on a ground fault. It is necessary to arrange the scheme so that even though the phase relay operates on a ground fault simultaneously with the ground relay and the ground-phase selector, the phase-relay trip circuit cannot be established. This is done by the inclusion of the back contacts, S_A, S_B, and S_C, which are located on their respective phase-selector elements. In other words, for a phase-*A*-to-ground fault, D_o, I_o, *RRG*, and S_A contacts close to trip the trip coil *A*, and at the same time the S_A back contact opens and prevents the phase-relay contacts, *Z* and *D*, from picking up the *MC* relay which would attempt to trip all three poles. In some applications these phase-selector relay back contacts would not be necessary, since the response of the distance-type phase relay to close-in ground faults depends upon the actual ohm setting of the phase relay and the magnitude of the ground current.

It is seen, therefore, that the additional contacts fundamentally necessary for single-pole tripping consist of the three-phase-selector make contacts which permit the splitting of the conventional ground-relay circuit into three parts. Depending upon the type of phase relay with which single-pole tripping is used, it may be necessary to employ three auxiliary blocking contacts to prevent interference by the phase relays.

The complete schematic connections are shown in Figure 6. The additional contacts which have been added over and above those shown in Figure 5 are the Z_1, Z_3, T_2, and T_3 contacts which will be recognized as being connected in the conventional manner for one of the present standard distance-type carrier systems.[4] No attempt is made here to describe the function of these contacts, as this has been done previously. The significant additional contacts are FD_a, FD_b, and FD_c. These are purely precautionary contacts which have been added in parallel with the back-contact phase-selector contacts, S_A, S_B, and S_C, respectively, for the purpose of preventing incorrect action caused by errors in the current transformers. It will be noted that on phase-to-phase faults not involving ground there is no zero-sequence current present. Therefore, no torque appears in the phase-selector elements, and thus the back contacts, S_A, S_B, and S_C, should remain closed. However, due to errors in the current transformers a small current might appear in the zero-sequence coils of the phase-selector relay. The back contact may, therefore, be inadvertently opened, and this would, of course, prevent tripping by the proper phase relay. The *FD* contacts are back contacts on an auxiliary fault-detector relay energized by the zero-sequence current and set for a current sufficiently above any anticipated error current in the current transformer. If, therefore, any of the back contacts, S_A, S_B, and S_C, are momentarily opened by the current-transformer error current, the circuit

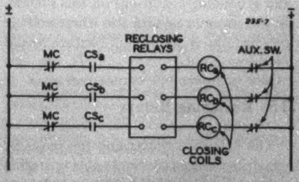

Figure 7. Simplified schematic diagram of reclosing circuits

Table I. Results of 132-Kv Breaker Tests

Public Service Company of Indiana, March 16, 1941

		New Castle			Lenore		
Test	Fault	Kva (Equiv. 3-Phase)	Breaker Interrupting Time (Cy.)*	Breaker Reclosing Time (Cy.)	Kva (Equiv. 3-Phase)	Breaker Interrupting Time (Cy.)*	Breaker Reclosing Time (Cy.)
1	C-Gr. (arc)	310,000	4.0	22.75	190,000	6.25	28.75
2	C-Gr. (solid)	300,000	3.5 (4)	22.5	190,000	5.6 (5.75)	29
3	C-Gr. (arc)				180,000	6.0	29
4	C-Gr. (solid)	50,000	4.5 (4.3)	22.5	415,000	6.2 (4.25)	29
6	B-C-Gr (solid)	170,000	4.25		380,000	6.0	
7	B-C-Gr (solid)	70,000	4.75		230,000	6.3	
9	B-C (solid)	300,000	3.9		290,000	8.25	
16	A-Gr (arc)	240,000	3.75	24.5	190,000	6.3	29.9
17	A-Gr (arc)	260,000	3.5	24.9	190,000	6.4	30

* Interrupting times given in parentheses are for second opening where breaker did not stay in after reclosure.

is still maintained through the fault-detector contact. On any fault involving ground, the fault-detector contacts open and leave the blocking function entirely up to the phase-selector back contacts.

In the Indianapolis installation a condition obtains which requires the addition of a blocking contact, *X*3. In this case the ground relay at both ends of the line was polarized by power-transformer bank neutral current and while one phase wire is disconnected, a zero-sequence current circulates in such a manner as to give the directional element, D_o, at each end of the line a fault indication. In other words, while one phase wire is open, the directional elements at both ends close, thus preventing a carrier signal from being transmitted, and this, of course, results in a tripping indication during the time one phase wire is disconnected. In order to prevent the immediate retripping of the breaker upon reclosure, the *X*3 contact was inserted. The *X*3 relay is energized at the same time that the trip current is established in the ground relay through the operation of the *CS* auxiliary relay whose contact energizes the *X*3 relay coil. The *X*3 relay picks up and opens its back contact with a time delay of about two cycles and resets with a time delay such that its back contacts become closed again a few cycles after the breaker pole has been reclosed. The choice of this *X*3 blocking contact was demanded by the fact that the ground-relay directional element was current-polarized and by the particular load and grounding conditions on this system. In the general case and also where voltage polarization is used, this contact would not be required.

The Reclosing Relays

The problems concerning the reclosing relays introduced by single-pole operation are simple of solution. The reclosing relays used on the Indiana installation were of the conventional type, and nothing of a particularly novel nature was required. Therefore, for the sake of simplicity, these reclosing relays have been grouped in the box of Figure 7. Each breaker-closing coil is energized through the make contact of auxiliary switches, CS_a, CS_b, and CS_c, which are operated whenever tripping occurs through the phase-selector contacts, S_a, S_b, and S_c. If phase-*A* conductor is faulted, the CS_a contact will close and energize the phase-*A*-breaker closing coil. The *MC* back contacts are on the *MC* relay (Figure 5), which is operated whenever a phase relay initiates tripping. Thus, on any phase fault the opening of the *MC* contacts prevents the energization of any of closing coils. To accomplish the feature of three-pole tripping after reclosure of a single pole on a solid fault merely requires the addition of auxiliary relays to the conventional reclosing relays and is not shown, since nothing novel is involved.

The Breakers

The fault having been detected and identified as one requiring a single-pole breaker operation, the next step in maintaining system stability is handled by breakers differing only slightly from the conventional type.

At the New Castle station of the Indiana Public Service Company a new breaker has been installed, each pole independently operated for either closing or tripping and so arranged that upon demand it will reclose within 30 cycles (0.50 second) after the trip coil has been energized. The operating mechanism is a solenoid, a unique feature of which is its "magnetic brain."

For fast reclosure it is desirable that the solenoid be nonmechanically trip-free; that is, that energy applied to the closing coil will arrest the opening movement of the breaker and return it to the closed position without the delay necessary with a trip-free linkage. The latter, of course, requires that the mechanical tie between two levers is broken by the action of the tripping latch and must be restored, as by retrieving springs, before the closing core can again be used to close the breaker. This means that the breaker opens practically its entire stroke, and the over-all time consumed becomes excessive. At the same time, there are circumstances under which trip-free operation is desirable, as when the breaker is to be tripped while still under the influence of pull of the closing magnet. It is here that this magnetic interlock performs, and with only one trip magnet, makes a choice between two latches, one of which opens the breaker "nontrip-free" for fast reclosure from the closed position, while the other provides "trip-free" operation in case the closing coil is still energized.

The operation of this magnetic interlock centers about a floating bar above the trip magnet, either end of which can be blocked to form a fulcrum point and cause the opposite end to rise when the trip coil is energized, thus providing for proper selection between the two latches. The choice of a proper point for blocking this bar is determined by an armature piece located within the field of flux of the closing magnet, but biased by springs to such a position that it releases the "nontrip-free" latch normally. Only when there is some magnetism in the closing coil, is it pulled to the opposite position to permit tripping on the "trip-free" latch.

The electrical connections for this operating mechanism remain as simple as for any of the solenoids in use for many years, as there is only one trip coil. The usual number of auxiliary switches is provided, or this can be amplified as desired if added circuits for other relays are needed.

The entire operating mechanism with control relays is mounted in a weatherproof sheet steel housing and attached to the tank wall, one such assembly for each pole of the three-pole breaker. Within this housing are also mounted the terminal blocks for bushing-type current-transformer secondaries. It is possible to make connections to any of the several transformer taps as required, at this one convenient location, and all conduit to the breaker is brought into the one house, whether intended for transformer secondaries or for control.

The scheme of single-pole operation has been worked out for revamping old three-pole breakers as well as for new apparatus. The experience at the Lenore end of this line demonstrates that the operation can

High-Speed Single-Pole Reclosing

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THE first application of high-speed single-pole reclosing to a high-voltage transmission line was recently made, tested, and put in service, on a 50-mile section of 138-kv single-circuit line on the system of the Public Service Company of Indiana, Inc. This paper discusses the reasons for choosing single-pole reclosing, and the installation and field testing of the equipment made under operating conditions. A companion paper by S. L. Goldsborough and A. W. Hill[1] describes the relaying and circuit-breaker equipment in detail.

When a single-circuit transmission line is used to interconnect two major systems and the line is used to transmit firm power between systems, considerable dependence must be put on the performance of this line. Under such circumstances unusual precautions are justified:

1. To prevent faults occurring on the line.
2. To minimize the disturbances created by faults.

The first objective is approached by using good line construction, by designing the line to be as nearly lightning proof as is economically possible, sometimes by the use of protector tubes, or possibly by the use of ground-fault neutralizers to prevent line-to-ground arcs from developing into faults.

It is not economical to design lines so that faults will never occur, although this ideal may be approached. Granted that a certain number of faults will occur, it is important to minimize their effect on the interconnected system. The objective when a fault develops is to remove the fault and re-establish the circuit without losing synchronism between the systems, so that there is no interruption in service over the interconnecting line.

Since the concepts of power-system stability were first recognized and the various factors influencing stability studied, means have been sought to improve the performance of power systems during transient disturbances.[2] A multiplicity of measures are now recognized, most of them directed toward minimizing the severity of the fault. During earlier years attention was particularly focused on improvements in stability limits obtained by design characteristics of machines and transformers, bussing arrangements, location of intermediate switching points, and the design of the transmission line itself. In the last decade high speeds for relay and circuit-breaker operation have become practical and extensively applied. High-speed apparatus, minimizing the duration of transmission-line disturbances, has been a major factor in improving system performance

The use of reclosing circuit breakers provides a means for furthering the advantages made possible by high-speed breakers and relays used for rapid fault isolation. The fundamental idea of reclosing circuit breakers was conceived by F. E. Picketts and filed with the United States Patent Office in 1916. Reclosing is particularly advantageous in the case of single-circuit transmission lines, for, while high-speed apparatus serves to remove the fault in the quickest possible time, loss of synchronism between the sending and receiving systems will quickly result if the line remains open. If loss of synchronism is not to occur, not only must the fault be cleared promptly, but the line must be restored to service after the fault is removed before the two systems have drifted far enough apart to cause instability.[3,4] High-speed reclosing has been used successfully, recently with simultaneous tripping and reclosing of the three-breaker poles.[5–8] This type of operation is known as gang-operated or three-pole reclosing. For a given time of reclosure the maximum power which may be transmitted without loss of synchronism depends on the relative inertias of the two systems (which determines the speed at which the systems drift apart when a particular fault is on, or when the line is open) and the initial operating angle between the systems. The latter is primarily a function of system reactance and transmitted power.

The time that the line can remain open without loss of stability is in most

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The authors acknowledge the fundamental work done by E. L. Harder on transient power limits obtained by the use of single-pole reclosing, the work done by S. B. Griscom on arc deionization time, and the work of H. M. Smith, Jr., in performing the calculations necessary to the preparation of the paper.

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be practically as fast even with fifteen year old breakers. In this case the contact structure of the old breaker was modernized with "De-ion Grids" to get the benefit of modern high-speed interrupters and, particularly, to have a device well able to withstand instantaneous reclosure.

The results of the recent tests on the Indiana Public Service system show the benefits of single-pole reclosure. Breaker reclosing times of from 22.5 to 30 cycles were secured.

Conclusions

1. The fundamental relay problem introduced by single-pole tripping and reclosing concerns an adequate sensitive means of selecting the faulty phase. This problem has been solved by utilizing the phase-angle shift between two sets of phase-sequence quantities.

2. An actual installation of a phase-selector relay co-ordinated with a distance-type carrier scheme has been made and tested with very satisfactory results.

3. It was observed that the breakers showed no outward display during any of the tests, indicating the suitability of the combination of modern "De-ion Grid" interrupters and fast-reclosing solenoid mechanisms for this duty. Adequate contact travel was secured before the breaker was reclosed, thus providing ample time for the fault arc to become de-energized, without crowding these operations to the limit as would be required for maintaining stability when opening all three poles.

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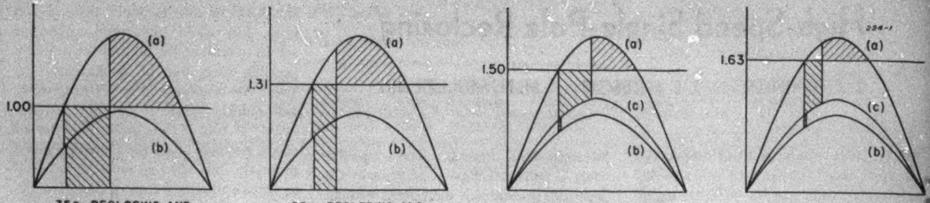

35∿ RECLOSING AND GANG-OPERATED BREAKER

20∿ RECLOSING AND GANG-OPERATED BREAKER

35∿ RECLOSING AND SINGLE-POLE BREAKERS

20∿ RECLOSING AND SINGLE-POLE BREAKERS

Figure 1. Comparison of transient power limits on a hypothetical system using gang-operated and single-pole 8-cycle breakers for reclosing speeds of 35 and 20 cycles

(*a*) Sending-end power-angle curve before fault occurs

(*b*) Sending-end power-angle curve during single line-to-ground fault

(*c*) Sending-end power-angle curve with one phase switched out of service

cases very short, thus high speeds of circuit-breaker reclosing are dictated. The reclosing time is measured from the time the breaker trip coil is energized until the breaker has opened, and the contacts are again closed. Commercial breakers are available today with reclosing times of 35 and 20 cycles at 138 kv on a 60-cycle per-second basis, and several installations employing breakers using such reclosing speeds are in service. The limit to which the reclosing time may be shortened depends upon breaker opening time and the speed attainable with reclosing mechanisms. One other factor, the actual deionizing time of the arc is of primary importance, since nothing will be gained if the circuit breaker is reclosed before the arc path has deionized, for the fault would then restrike after reclosure. Table I gives approximate data (obtained from tests on typical systems) of the minimum reclosing time permissible without re-establishment of the arc. These deionizing times are of course variable, depending upon line design, wind conditions, and so on, and the figures of Table I are to be taken as typical of expected results under average conditions.

The foregoing discussion has assumed simultaneous tripping and reclosing of all three circuit-breaker poles which will be referred to in this paper as gang-operated or three-pole reclosing. In all discussions of high-speed reclosing pertaining to line sections having sources of power at both ends, simultaneous operation of the circuit breakers at both ends of the transmission line is assumed, since, otherwise, the de-energized time of the line would be shortened, and the possibility of restriking would be increased. Simultaneous tripping is accomplished by pilot-wire or carrier-current relaying or, when possible, by setting high-speed impedance-type relays to cover the entire line section.

Utilizing high-speed relays and gang-operated breakers and the high speeds of reclosure mentioned above, cases will arise in which the desired power cannot be transmitted without loss of stability during fault conditions. A step toward raising the transient power limits, beyond three-pole reclosing, may be achieved by single-pole reclosing of circuit breakers. Single-pole reclosing consists of opening only the breaker pole connected with the faulty phase and reclosing this pole after the fault has cleared. Single-pole reclosing has application only to grounded-neutral systems. An increase in the transient stability limit is realized as compared with gang operation, since a considerable amount of power may be transmitted over the sound phases while the faulty phase is isolated. For a given amount of power transmitted over a particular transmission line, the permissible de-energized time to maintain synchronism is appreciably longer, if single-pole reclosing is used as compared with three-pole operation. Hence, in very high-voltage systems where speeds too fast for adequate arc clearing might be required with gang operation, slower speeds, providing ample time for arc clearing may maintain stability conditions if single-pole reclosing is used.

The actual gain in transient-stability limit realized with single-pole operation can be visualized by reference to Figure 1. This figure shows power-angle diagrams for a typical system, illustrating the relative transient-stability limits obtained with 35- and 20-cycle, three-pole and single-pole reclosing. The curves were drawn for a hypothetical single-machine station feeding a high-voltage line terminating in a very large system, assumed to have infinite inertia. The curves serve to illustrate qualitatively the expected increase in transient limits and the relatively large amounts of power which can be transmitted over the unfaulted phases while one phase is open.

Table I. Typical Arc Deionizing Times

Nominal System Voltage (Kv)	Deionizing Time for 95% of Faults (Cycles on 60-Cycle Basis)	Minimum Permissible Reclosing Time With Eight-Cycle Breakers	Minimum Permissible Reclosing Time With Five-Cycle Breakers
23	4	12	9
46	5	13	10
69	6	14	11
115	8.5	16.5	13.5
138	10	18	15
161	13	21	18
230	18	26	23

Application

The principal transmission lines of the Public Service Company of Indiana, Inc., including the major interconnections with neighboring companies, are shown on the map of Figure 2. The principal generating stations of the Public Service Company of Indiana, Inc., are at Dresser and at Edwardsport. Considerable generating capacity is available at Indianapolis, through the interconnection with the Indianapolis Power and Light Company, and from the south through interconnection with the Cincinnati Gas and Electric Company. Interconnection is made at Muncie and Kokomo with the Indiana section of the American Gas and Electric system.

Figure 3 shows in greater detail the Lenore–Newcastle–Muncie interconnection installed during the past year which is the principal subject of this discussion. Approximate generating capacity at the major points of application to the interconnected system is indicated on Figure 3. The Indiana section of the American Gas and Electric system is extensive and includes a multiplicity of sources of generation, so that the generation at Muncie has not been assigned a definite capacity.

The Lenore–Newcastle line, a part of the new interconnection, was also built to supply firm power to the load at Newcastle, the required capacity of the line to be at least 50,000 kva. The Newcastle–

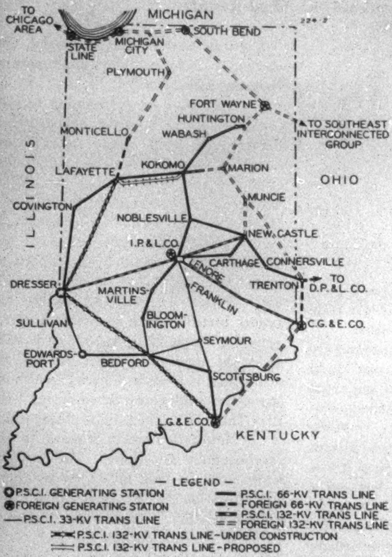
TO CHICAGO AREA
MICHIGAN
SOUTH BEND
STATE LINE
MICHIGAN CITY
PLYMOUTH
FORT WAYNE
HUNTINGTON
TO SOUTHEAST INTERCONNECTED GROUP
MONTICELLO
WABASH
ILLINOIS
LAFAYETTE
KOKOMO
MARION
OHIO
MUNCIE
COVINGTON
NOBLESVILLE
NEW CASTLE
I.P.&L.CO.
CARTHAGE
CONNERSVILLE
LENORE
DRESSER
MARTINSVILLE
FRANKLIN
TRENTON
TO D.P.&L.CO.
SULLIVAN
BLOOMINGTON
C.G.&E.CO.
SEYMOUR
EDWARDSPORT
BEDFORD
SCOTTSBURG
L.G.&E.CO.
KENTUCKY
— LEGEND —
P.S.C.I. GENERATING STATION
FOREIGN GENERATING STATION
P.S.C.I. 33-KV TRANS LINE
P.S.C.I. 66-KV TRANS LINE
FOREIGN 66-KV TRANS LINE
P.S.C.I. 132-KV TRANS LINE
FOREIGN 132-KV TRANS LINE
P.S.C.I. 132-KV TRANS LINE-UNDER CONSTRUCTION
P.S.C.I. 132-KV TRANS LINE-PROPOSED

Figure 2. Major high-voltage interconnected lines of the Public Service Company of Indiana, Inc. and neighboring systems

Muncie line was proposed to provide an interconnection, ordinarily floating, between the Public Service Company of Indiana, Inc., and the American Gas and Electric system. Under emergency conditions interchange of power in either direction is permitted. The Lenore–Newcastle line and the Newcastle–Muncie line are now single-circuit lines. The Lenore–Newcastle line is ultimately planned to be double circuit, as indicated by the tower structure shown in the photograph of Figure 4. The line is designed to have nine insulator units used in suspension and eleven insulator units used in strain. The single ground wire now installed on this structure is three-strand, $^{3}/_{8}$-inch galvanized steel conductor. The line conductors are 300,000 circular-mil copper. The line route is east and west over relatively flat terrain, and the tower-footing resistances test well under five ohms.

Other circuits parallel the new line from Indianapolis to Muncie, as shown in Figure 2. The existing ties are, however, relatively weak and of high impedance. If an interruption should occur on the Lenore–Newcastle line while an appreciable block of load were being transmitted over the line to Newcastle, the paralleling ties through Noblesville, Kokomo, and Marion would not be able to pick up the load, because of their high impedance, and instability would occur. Consequently, it is of prime importance to keep the Lenore–Newcastle line in continuous operation. Although the line is designed to be relatively lightning-proof, some interruptions of the Newcastle supply would inevitably result unless means were taken to re-establish the line immediately following an interruption, and before instability could occur. Consequently, high-speed reclosing was considered necessary to keep interruptions to the Newcastle load at an absolute minimum.

The 138-kv line from Newcastle to Muncie is provided with 20-cycle reclosing circuit breakers, 1,500,000-kva interrupting capacity, at each end of the line section. Conventional carrier-current relaying is used for both ground and phase relaying. The circuit breakers are conventional eight-cycle breakers and are equipped with gang-operated mechanisms.

Both gang-operated (three-pole) reclosing and single-pole reclosing were considered for the line from Lenore to Newcastle. Speeds of reclosing of 35 cycles and 20 cycles were considered. Table II gives the results of calculations made to determine the transient power limits for both speeds of reclosure considered and for both gang-operated and single-pole reclosing. Simultaneous tripping (and reclosing) of two circuit-breaker poles was assumed in the calculations for faults involving two-line conductors. With the type of construction used, a very high percentage of the faults will originate and persist as single line-to-ground. Primary consideration was consequently given to the transient power limits achieved during single line-to-ground fault conditions. Breakers and mechanisms for 35-cycle single-pole reclosing can be obtained at approximately the same price as breakers and mechanisms for 20-cycle gang-operated reclosing. The figures of Table II show considerably higher transient power limits during single line-to-ground faults for 35-cycle single-pole reclosing than the limits achieved using 20-cycle gang-operated reclosing. The power limits for 20-cycle single-pole reclosing are yet higher, but it was felt that the gain in power limits on this application did not justify the considerable increase in cost for higher-speed breakers and mechanisms. On the basis of this comparison, 35-cycle single-pole reclosing was adopted for the line section.

Existing 138-kv breakers at the Lenore end of the line were provided with 35-cycle single-pole reclosing mechanisms. A new breaker was installed at the Newcastle end of the line—a 138-kv, 8-cycle 1.5-million-kva unit, equipped with a 35-cycle reclosing mechanism on each pole. Carrier-current relaying is used to permit simultaneous operation of the breakers at the two ends of the line. The phase relaying is of conventional type, and it was necessary to develop a new ground-relaying scheme to select the faulted phase. The scheme for selecting the faulted phase is described in a companion paper by S. L. Goldsborough and A. W. Hill.[1] The equipment is designed to operate as follows:

> For single line-to-ground faults the breaker pole connected to the faulted phase at each

Table II. Calculated Transient Power Limits, for Single-Pole and Three-Pole Reclosing, at 20 and 35 Cycles, Using 8-Cycle Breakers With Carrier-Current Relaying

		Breaker Operation		Comparative Transient Power Limits	
Item	Type of Fault	Reclosing Time	Type of Reclosing	Megawatts	Per Cent*
1	Single line-to-ground	35	Gang......	74.5	100
2		20		95.8	129
3		35	Single-pole.	105.9	142
4		20		116.8	157
5	Double line-to-ground	35	Gang......	71.1	100
6		20		91.1	128
7		35	Single-pole.	81.3	114
8		20		97.3	137
9	Three phase	35	Gang......	70.4	100
10		20		90.1	128

*Transient power limit obtained with 35-cycle, gang-operated breakers taken as 100 per cent for each type of fault.

end of the line is tripped and reclosed. If the fault persists after reclosure, all breaker poles are tripped and locked out. All breaker poles are tripped and locked out for all faults involving more than one conductor.

On most future installations of single-pole reclosing it is expected that two poles will be tripped and reclosed for two-phase faults, and three poles tripped and reclosed for three-phase faults, but such operation is not necessary on the Lenore–Newcastle installation at the present time.

Figure 5 is a photograph of the new 138-kv breakers at the Newcastle substation equipped for single-pole reclosing.

Tests

Extensive field tests were made on the system of the Public Service Company of Indiana, Inc., on March 16, 1941 and were primarily intended to check the functioning of the relays and reclosing equipment. Two oscillographs were stationed at Lenore to record simultaneously the following quantities:

Phase currents in the Newcastle line.
Residual current in the Newcastle line.
Carrier signals at Lenore.
Lenore 138-kv bus voltages.
Phase *C* voltage on the line side of the Newcastle line breaker at Lenore.
Phase *C* current in the Dresser line.
Phase *C* current in the Indianapolis Power and Light Company line.
Phase *C* current in the Lenore condenser.
Phase *C* current in the Columbia line.
Residual current in the Lenore transformer neutrals.
Travel for the reclosing breakers.
Trip coil currents for the reclosing breakers.

Two oscillographs were stationed at Newcastle to record the following:

Phase currents in the Lenore line.
Residual current in the Lenore line.
Residual current in the Muncie line.
Residual current in the Newcastle transformer neutral.
Carrier signals at Newcastle.
Phase *C* voltage on the line side of the Lenore breaker.
Travel of the reclosing breakers in the Lenore line.
Trip coil currents of the Lenore and Muncie line breakers.

Additional oscillographic records were obtained by the American Gas and Electric Service Corporation at Muncie.

Faults were applied to the system at the points marked as F_1, F_2, F_3, and F_4 in Figure 3. The fault locations and types of faults were selected to provide a comprehensive test for the relays, reclosing mechanisms, and breakers associated with the Lenore–Newcastle line. Since it was necessary to run the staged tests on a Sunday, during system light-load conditions, the tests could not indicate directly the ultimate transient-power limits which can be achieved. Indirectly, however, the records should provide a very good indication of stability conditions by showing the disturbance to the system initiated by the fault.

The complete test schedule is outlined in Table III. Arcing faults were initiated by closing a circuit breaker which connected a line conductor to ground through a fine wire suspended across an insulator string. The arc resulting when the switch was closed is shown in Figure 6. Solid faults were also initiated by the closing of a breaker.

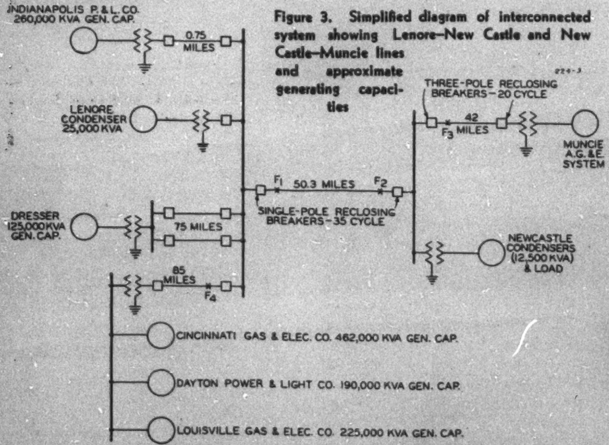
INDIANAPOLIS P. & L. CO.
260,000 KVA GEN. CAP.
0.75 MILES
LENORE CONDENSER 25,000 KVA
DRESSER 125,000 KVA GEN. CAP.
75 MILES
85 MILES
F_4
F_1
50.3 MILES
F_2
SINGLE-POLE RECLOSING BREAKERS – 35 CYCLE
224-3
THREE-POLE RECLOSING BREAKERS – 20 CYCLE
42 MILES
F_3
MUNCIE A.G. & E. SYSTEM
NEWCASTLE CONDENSERS (12,500 KVA) & LOAD
CINCINNATI GAS & ELEC. CO. 462,000 KVA GEN. CAP.
DAYTON POWER & LIGHT CO. 190,000 KVA GEN. CAP.
LOUISVILLE GAS & ELEC. CO. 225,000 KVA GEN. CAP.

Figure 3. Simplified diagram of interconnected system showing Lenore–New Castle and New Castle–Muncie lines and approximate generating capacities

Test Results

The tests were entirely successful, the single-pole reclosing relays and breakers operating satisfactorily in all tests. Typical of the performance recorded for all tests, the Newcastle breaker in tests 1 and 2 interrupted 1,300 amperes in six cycles after initiation of the fault and four cycles after energization of the trip coil. The line remained de-energized 19 cycles before reclosure, giving a reclosing time of 23 cycles. The breaker remained closed on the arcing fault of test 1 but tripped a second time and locked out on the solid fault of test 2. The reclosing times for the Lenore breakers were somewhat longer, averaging about 29 cycles, but still well under the nominal 35 cycles. A typical oscillogram is given in Figure 7, which is one of the oscillograms taken during test 12 of Table III. The total reclosing time of the Newcastle breaker is shown to be approximately $25^1/_2$ cycles. The breaker interrupted the fault in $5^1/_2$ cycles after fault inception and $3^1/_2$ cycles after trip-coil energization. Trace number 9 shows a flow of residual current for 31 cycles, indicative of the reclosing time at Lenore. The times to arc extinction and reclosure were remarkably consistent on all tests. It is of interest that in test 3 the three-pole 20-cycle reclosing breaker at the New-

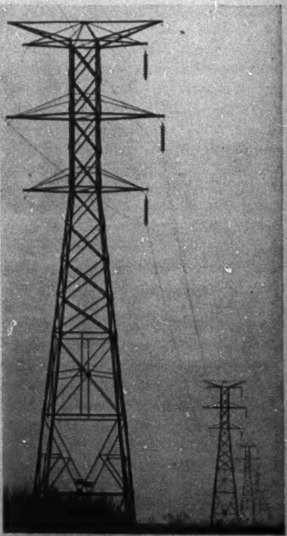

Figure 4. Tower construction and conductor arrangement of Lenore–New Castle 132-kv line

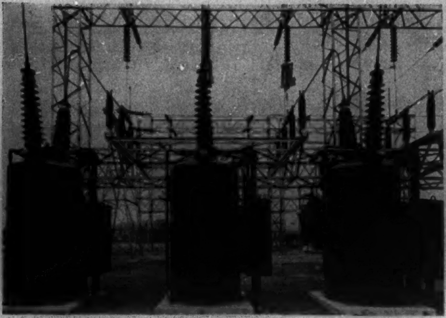

Figure 5. Close-up of 132-kv single-pole reclosing breakers at New Castle substation

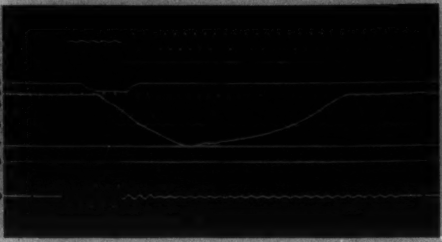

Figure 7. Typical oscillogram showing single-pole reclosing-breaker operation for a phase-*A*-to-ground arcing fault

1. *A*-phase bus potential
2. *A*-phase line current
3. *A*-phase breaker trip current
4. *A*-phase breaker travel
5. *B*-phase bus potential
6. *B*-phase line current
7. *B*-phase breaker trip current
8. *B*-phase breaker travel
9. Lenore line residual current

castle end of the Newcastle–Muncie line reclosed on the arcing fault on the Muncie line in approximately 17 cycles (because of adjustment for faster reclosing speed than necessary) and the arc restruck. This performance may indicate that times of reclosing less than 20 cycles cannot be used on 138-kv systems.

The disturbance to the remainder of the system on all tests was negligible. On test 12 the total Newcastle load, 40,000 kw, was supplied over the Lenore–Newcastle line, with all paralleling ties open. No disturbance was noted at Newcastle or other points on the system and the oscillograms revealed that very little swinging of the Newcastle condensers and other connected machines occurred. During this test approximately 500 amperes residual current appeared at the inception of the fault in the Newcastle line at Lenore. When the faulted phase was opened, before reclosure, the residual current in the line at Lenore decreased to 85 amperes. The residual current in the Muncie line, during the period that the faulted phase was de-energized, was so small that it could not be accurately scaled from the oscillograms but was well under 50 amperes.

Figure 6. Arcing fault initiated by connecting a fine wire from line to ground

General Discussion

The field tests demonstrated that the relaying scheme developed to select the faulted phase, trip the breaker pole connected to the faulted phase, and reclose the breaker, functioned properly for both arcing and solid faults. The scheme also operated properly for single-phase faults and for faults involving more than one phase. The Lenore–Newcastle line has not had an automatic-breaker operation, other than during the test program, since the line was put in service even though a rather severe lightning season has been experienced, and neighboring lines in the same territory have suffered several interruptions. The past year's experience indicates the adequacy of the line design to minimize the occurrence of faults caused by lightning.

Calculations indicate that single-pole reclosing results in considerably higher transient power limits than can be achieved with three-pole reclosing, if due consideration is given to the high percentage of single line-to-ground faults, the relatively small percentage of faults involving two-phase conductors, and the very small percentage of three-phase faults occurring on a well-designed transmission line equipped with apparatus to provide high-speed clearing of faults. The curves of Figure 8, calculated for an assumed system consisting of a 50,000-kva waterwheel generator feeding into an infinite system (compared to the 50,000-kva station) over a single 138-kv line, illustrate the relative transient-power limits attained with single-pole reclosing for single line-to-ground faults (curves *a*), double line-to-ground faults (curves *b*), and three-phase faults (curves *c*). Two assumed speeds of operation were chosen for the calculations: 4-cycle fault clearing with 20-cycle reclosing (the solid curves), and 8-cycle fault clearing with 35-cycle reclosing (the dotted curves). Length of line connecting the systems was varied to show the effect of change in system "through" reactance. Similar curves calculated for a 250,000-kva steam turbine-generator station feeding into an infinite system over a 138-kv line are given in Figure 9. It is not strictly correct to compare curves *a* and *c* in these figures, for example, to show the improvement in power limits obtained by using single-pole rather than three-pole reclosing, since curves *a* assume a single line-to-ground fault and curves *c* assume a three-phase fault. The error in direct comparison lies only in the fault severity before the fault is cleared, a factor largely concealed by the number of phases opened before reclosure and the time of de-energization of the line.

In the above paragraphs the principle of single-pole reclosing has been described and its advantages with respect to transient stability pointed out. Other advantages accrue from its use, but single-pole reclosing must not be considered as a panacea for all transmission-line problems. Other methods of improving transmission-line operation with respect to transient stability limits and service continuity are available, and the choice of apparatus or method will be a function of several variables in each individual case.

The use of ground wires to prevent transmission-line outage has widespread acceptance. It is not economical to de-

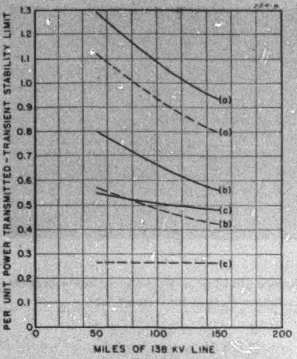

Figure 8. Comparison of transient stability limits with 35- and 20-cycle single-pole reclosing for a 50,000-kva water-wheel generator feeding an infinite system over a 138-kv line

——— 20-cycle single-pole reclosing
- - - - 35-cycle single-pole reclosing

(a) Single line-to-ground fault
(b) Double line-to-ground fault
(c) Three-phase fault

sign lines to completely eliminate fault occurrence, although in the higher-voltage classes, this ideal may be approached. If a line is being operated near its stability limit, or if the few faults occurring on a well-designed line are objectionable from the standpoint of service continuity, high-speed reclosing, either gang or single pole operated, may be used to advantage. It is expected that single-pole reclosing will find considerable application on existing transmission lines, even those lines designed to be relatively lightning proof, particularly as line loads increase and the present transient stability limit is approached. On new lines, the economic choice between high-speed reclosing and other means for improving performance will depend upon line length, soil conditions, stability considerations, line costs, and so on.

Single-pole reclosing can be compared to the result achieved with the ground-fault neutralizer in that its salient advantage applies for single line-to-ground faults. On well-designed high-voltage transmission lines a large percentage of all faults are single line-to-ground in nature, and practically all faults originate as single line-to-ground. Single-pole operation to clear single line-to-ground faults will, in most cases, include two-pole operation for double line-to-ground faults and gang-operated reclosing for phase-to-phase faults. This extension leaves one pole at each end of the line connected during the double line-to-ground fault to achieve some increase in transient stability limits. The installation described in this paper does not include individual pole operation for any faults other than single line-to-ground for reasons indicated above.

Table III. Schedule of Single-Pole Reclosing Tests, Public Service Company of Indiana March 16, 1941

Test No.	Time	Fault Location*	Type of Fault	System Conditions
1	8:01 a.m.	F_2	Phase C to ground-arcing	Normal megawatts, Lenore to Newcastle. Muncie line closed
2	8:57 a.m.	F_2	Phase C to ground-solid	Normal
3	9:40 a.m.	F_2	Phase C to ground-arcing	Normal
4	11:23 a.m.	F_1	Phase C to ground-solid	†I.P. and L. open, Muncie–Newcastle open
5	11:43 a.m.	F_4	Phase C to ground-solid	†I.P. and L. open, Columbia open at Columbia
6	12:20 p.m.	F_1	Phases B and C to ground-solid	†I.P. and L. open
7	12:52 p.m.	F_2	Phases B and C to ground-solid	†I. P. and L. open, Muncie – Newcastle open
8	1:31 p.m.	F_3	Phases B and C tied solidly	Normal
9	2:10 p.m.	F_3	Phases B and C tied solidly	Normal
10	2:35 p.m.	F_3	Phase C to ground-solid	Normal
11	3:16 p.m.	F_2	Phase A to ground-arcing	All paralleling ties open, 11 megawatts from Lenore to Newcastle
12	3:31 p.m.	F_2	Phase A to ground-arcing	All paralleling ties open, 40 megawatts from Lenore to Newcastle

*See Figure 3.

†Indianapolis Power and Light Company.

Compared with ground-fault neutralizers, single-pole reclosing clears ground faults without the necessity of operation with two-phase wires at full line-to-line voltage above ground, saves the cost of neutralizers, and permits the use of grounded-neutral transformers and lightning arresters so that better lightning protection of equipment connected to the transmission system is attained. During single line-to-ground faults, however, single-pole reclosing involves unbalances, and as much power cannot be transmitted during a line-to-ground fault condition as can be transmitted with neutralizers. During faults involving more than one phase, the reclosing provides a possibility of maintaining stability not enjoyed by the neutralizer system. It is probable, in view of the above comparison, that single-pole reclosing will find preference over neutralizers, particularly on major high-voltage, single-circuit, interconnecting lines.

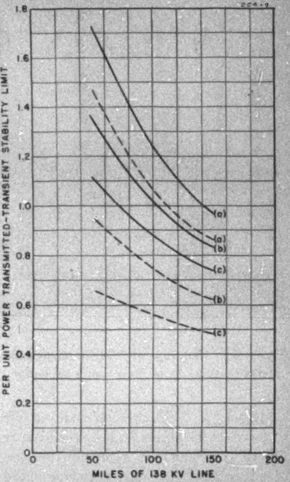

Figure 9. Comparison of transient stability limits with 35- and 20-cycle single-pole reclosing for a 250,000-kva turbine-generator station feeding an infinite system over a 138-kv line

——— 20-cycle single-pole reclosing
- - - - 35-cycle single-pole reclosing

(a) Single line-to-ground fault
(b) Double line-to-ground fault
(c) Three-phase fault

As a general comparison for a single-circuit line, the conventional system has zero-power transmitting ability through all faults; the neutralizer system will have no outages for transient single line-to-ground faults (about 70 per cent of all faults), but will result in outages for all other faults; single-pole reclosing can be applied to have no outages for all self-clearing faults. The advantages of single-pole reclosing over three-pole reclosing are minimized if the transmitted power is kept low enough so that no outages occur for self-clearing three-phase faults, although there is still the advantage of less shock to the system during the more common single-phase and two-phase fault conditions. The higher the probability of single line-to-ground fault occurrence, compared to multiphase faults, the greater is the advantage of using single-pole reclosing. High-speed reclosing can be used, either three-pole or single-pole, to prevent outages for all types of self-clearing faults, a very high percentage of all faults.

In all applications of high-speed reclosing, the desired power limit, the probability of fault occurrence, the probability of the type of fault, and the percentage of time during which the line is expected to operate at the maximum stability limit should be considered in determining the speed of reclosure and the type of reclosing (three-pole or single-pole) to be applied.

Although this discussion has been confined largely to the application of reclosing on single-circuit lines, it should not be inferred that reclosing (both three-pole and single-pole) does not have application to multicircuit systems. The use of reclosing on power lines can be considered as an alternative to the addition of parallel circuits to obtain a desired transient stability limit. In many cases, this will result in large savings of capital investment. Single-pole reclosing shows up to particular advantage on multicircuit systems, for opening one conductor to clear a single line-to-ground fault, in effect merely inserts a higher impedance in one phase. Ground relaying on lines paralleling the faulted line must be arranged not to trip on the ground current circulated during the interval when one or two phases are de-energized, although relays will usually not trip for this condition if they are adjusted not to trip during the fault. The ground current, while persisting for several cycles, will usually be small—depending upon the load transferred over the lines, line impedances, and so on.

The two principal objections offered to the use of single-pole reclosing, namely, the possibility of telephone interference created by the flow of ground current during the period that one phase is de-energized, and the possibility of false ground-relay operation in adjacent line sections caused by the circulation of ground current during the de-energized period, were not encountered in this installation. It is anticipated that neither factor will be objectionable in most contemplated installations, since the ground current resulting from the open phase is small and persists for only a short time. The disturbance to other ground relaying on the system is quite small if good ground sources are available at both ends of the faulted section.

The Dresser–Lafayette line shown in Figure 2 is now under construction. The breakers for this new line will be equipped for individual reclosing mechanisms, and relaying for single-pole tripping and reclosing will be added when the 132-kv bus is established at Lafayette. Another new 132-kv line—to extend from Dresser south to Bedford, Indiana, and from there to Louisville, Kentucky—will be similarly equipped for single-pole reclosing. This line will constitute a major interconnection between the Public Service Company of Indiana, Inc., and the Louisville Gas and Electric Company.

Conclusions

On high-voltage systems where most transmission-line faults are single line-to-ground in nature, single-pole reclosing provides higher transient-power limits than gang-operated reclosing. This permits the transmission of more power over given line sections, and permits slower speeds of reclosure on very high voltage systems where adequate time allowance for arc clearing becomes of importance. If the design of a particular line is such that a larger percentage of all faults will be double line-to-ground, the advantages of single-pole reclosing over gang-operated reclosing are not as pronounced. Single-pole reclosing will find application on multicircuit as well as single-circuit lines. Entirely satisfactory relay and breaker operation is indicated from comprehensive tests made in the field.

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Evening Courses at Graduate Levels—a Challenge to Colleges of Engineering

ROBIN BEACH
FELLOW AIEE

DURING the past months with the hysteria of war about us and with the nation struggling to convert its peacetime activities into a gigantic war-time arsenal of fighting and defense implements, the most urgent demands have been made for a trained personnel. The present dearth of technically trained workers and skilled artisans has been strongly felt in nearly every phase of this controversion of industry—ranging from the highest levels of engineering research and design, on through the vast varieties of supervisory positions in manufacture and inspection of materiel, and into the front line trenches of industry where plowshares are being turned into proverbial swords.

Suddenly, in the dismay of a nation awakened to its utter weakness in national defense because of its years of disregard of preparedness, overnight efforts were launched to gear industries into high-speed production of war machines and their munitions, and to multiply national output many times beyond normal. Leaders of industry sought on every hand, among the millions of unemployed, the highly skilled and trained personnel by which to double and treble their plants and to multiply their manufacturing operations. Obviously, and naturally, these leaders met with disappointments on every hand, as they soon found that the personnel which they sought, already trained, was not to be had.

War Demands on Training and Education

Then came the boom in education and training, "the gold rush," if you please, to the classroom, the laboratory, the drafting and design training rooms. On every hand, in every nook and corner, national defense training courses sprang into being, some of which were fair, some indifferent, and some worthless. An attempt was here being made to do the impossible—to make up, in a few short weeks or months, for the neglects—the depreciations—in the discarded ranks of labor for the prior years of disuse of skilled hands and trained minds.

For the first time since the halcyon days of 1928 and 1929, the engineering students, soon to be graduated, were eagerly sought, with few questions asked as to their scholastic attainments. Loud lamentations were heard because double and treble the number of graduates were not available. Proposals to shorten college curricula were urged in some quarters, and in other quarters even outstanding junior students were ill-advisedly enticed away from their engineering courses.

During this extreme urgency of employing all available engineering graduates, the members of the day classes were, for the most part, the ones who were readily available. The prospective employers found these well-prepared young men virtually in the ranks of the unemployed, or at least among the nonproducers. In consequence, their number added tangible substance to the personnel of industry. Now, if you please, let us turn our attention to another source of technical graduates—a supply of considerable and growing numbers, the graduates of evening sessons from the many colleges of engineering throughout the urban areas of our land.

Perhaps in this emergency these young men do not appear to personnel recruiting agencies to stand forth as prospective "knights in shining armor" because they have, long since, been employed in industry and, in consequence, they are already in harness, sharing responsibilities effectively and intelligently with their fellow workers as a result of their educational attainments. Of these graduates from evening sessons in engineering, and of the facilities for their training, we hear much less than of the conventional facilities for the instruction of day students.

Night Courses in the New York City Area

Here in the metropolitan New York area seven colleges of engineering have been offering evening courses leading to baccalaureate degrees. Of these the Polytechnic Institute of Brooklyn has had a long career in its service to the young men of this area, having pioneered in this type of engineering education in America, and having organized itself for this additional sphere of activity as early as 1904. The curricula and the standards of the day and the evening sessions are identical.

In those urban communities where the concentration of industry is relatively dense, obviously large numbers of young technical graduates are found among the personnel who are engaged in engineering supervision, design and planning, of industrial research, and of semi-engineering activities of countless types. The more ambitious of these feel a challenge offered to their abilities by the increasingly complex problems which they encounter in the rapid march of technological progress. They find need for more specific knowledge of engineering science in order better to cope with the growing intricacy of electrical systems with which they are engaged.

It is natural that they should turn to the engineering colleges near them in seeking advanced education to aid them in the more intelligent pursuit of their daily tasks. At this critical period of their professional lives, they know precisely wherein they lack certain educational facilities to cope with their new and advanced problems. It is then they need most the help which graduate study can give them. They have already become oriented in the field of their life's work, and perhaps quite a different one from that which they envisioned upon the day of graduation, so they can now choose more intelligently the character of advanced study which will serve them best.

The Organization of Graduate Courses

The Polytechnic Institute recognized this call for graduate instruction from these young men of industry as far back as 1925. In a community where so many utilities are engaged in the public service of electrical communications, of power and light, and of transportation, a course of basic and common interest to the many engaged in these various fields was that of advanced electrical-circuit analysis, in which the spectrum of frequencies considered was from commercial power values to those then employed in radiobroadcasting. From this small start, with but a handful of students, all eager to learn, appreciative of the opportunities offered,

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and hoping for additional facilities in such evening instruction, we launched upon our program of evening graduate study.

The results of our adventure into the then little explored field of evening graduate service have led to the development of the present programs of courses culminating in the master's and doctor's degrees in electrical engineering. It is of passing interest to note that the handful of students of the earlier years has grown into a registration of about 100 students of electrical engineering per year in continuous attendance throughout the period of the past decade. Of more recent years, other engineering colleges here in the New York area have likewise added evening courses of instruction at the graduate level to help in this service of affording the facilities of advanced education to the thousands of engineering graduates employed within our metropolitan area.

The programs of evening graduate study may be readily inspected by reference to the appropriate college catalogue. This listing of courses, citation of their contents, and enumeration of the assigned members of the teaching staff, whereas enlightening and informative, do not, however, give any indication of the problems encountered in the organization, administration, and growth of this graduate service, and of the different emphases in the philosophy of education as applied to this particular type of education.

With the hope that the review of some of these specific problems which confronted us may be of value or assistance to those who are now entering upon enterprises of a similar character, the author, with apologies for the close personal nature of these citations, ventures to describe those elements which played a dominant role in the evolution of our present electrical engineering evening curriculum.

After a brief experience with the instruction of these graduate evening students, several observations served us well in establishing subsequent procedures of organization and administration for this type of instruction. We found that these young men differed from the conventional graduate students of the day session in a number of essential characteristics. Most of them had the advantage of from three to five years, or more, of engineering experience which provided them with the motivating urge to develop further their mental facilities through evening study in advanced courses. These young men were matured, serious, and purposefully minded. They recognized in these opportunities for graduate instruction a possible means of enabling them to rise more rapidly above their present engineering status.

Many of them were engaged in various advanced phases of electrical engineering work, frequently of the industrial research type, wherein they discovered their knowledge to be deficient in meeting effectively, and with necessary dispatch, the problems of the day. They too often realized this disadvantage when attempting to study collateral bibliography because of their unfamiliarity with the various branches of advanced mathematics which they repeatedly encountered. Therefore, they sought a knowledge of advanced mathematics and a facility with its applications to a wide variety of electrical problems, as well as a thorough training in the more theoretical phases of electrical science—all to equip them with the ability to appreciate and to understand the literature in their respective fields. In addition, they expected that their increased knowledge at these higher levels of engineering and scientific attainments would provide them with the facility of technical expression by which they, in turn, would be enabled some day to contribute to engineering literature.

One of the prerequisites, or gratifications, granted to those of us who are in close association with this type of educational enterprise, is to observe the realization of the hopes of these young men to achieve such objectives and to actually contribute technical papers of commendable character to the various engineering and scientific periodicals. In fact, such achievements might well serve as an effective measure of the performance of these young men at the graduate levels of education.

The Instructor

It may appear trite and needless repetition to put into words the personal, industrial, and scholastic qualifications, the specifications, if you please, for instructors who contemplate this type of graduate teaching; but be assured that no phase of organization in this endeavor stands paramount to that of the selection of the most capable and highly qualified engineering educators. For the instructor of evening graduate students to enjoy the fruits of success in his endeavors, he should possess a rich, full educational background in the broader aspects of general engineering and, of course, he should be a master in the restricted confines of his primary interests. This should be supplemented with the invaluable experience to be gained from a period of engineering service in industrial practice in order to become intimately conversant with the varieties of problems of industry relating to his special field. He will then have a clearer perspective of the objectives of his instruction. The educational facilities of his courses will also serve his students better in meeting their urgent day-to-day needs, and they will also help to provide a more technically trained personnel for industry.

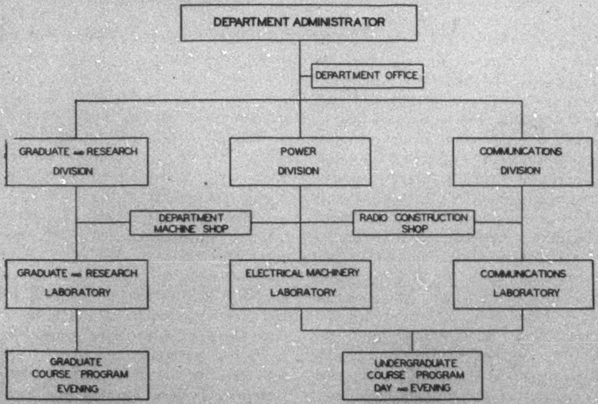
On the personal side, this instructor should have a cordial respect for the aspirations, the motivating influences, and the eagerness for learning of these evening students. His prestige as a scientist, an engineer, and an educator should invite a warmth of response from his class members. He should be their inspiring and cultural leader, and an enthusiastic proponent of this type of cooperative education. Nothing short of these essential attributes of the capable graduate instructor will meet with more than mediocrity in this challenging venture of higher education.

Those individuals in graduate engineering education who possess these requisite qualifications for teaching are indeed rare. This situation exists largely as a result of the natural and inexorable economic law of supply and demand. The prospective young teaching initiates—those exceptional young men of high scholastic attainments who can perform and think effectively at the doctorate level and who possess the many other attractive attributes of a cultured scientist-engineer are urgently importuned to remain in the industry wherein they are serving their internships in acquiring the practical experience prerequisite to teaching. Why shouldn't industry want to retain the services of such promising young men? If the graduate classrooms and research laboratories of our colleges of engineering are to be graced with the capabilities of these industrially groomed protégés of advanced engineering education, obviously such prospective young teachers can only be induced back to the campus by sufficiently attractive rewards. Industry pays them their current market worth. Do we expect them to sacrifice their careers in industry to teaching for less? Some apparently do.

Colleges have been known to accuse industry of unethical practices of competition when they have been frustrated in their attempts to employ these highly educated scientist-engineers by unattractive offers of salary. The problem appears to be one of justifiable competition in the open market of personnel. If we seek gems, we must be prepared to meet their market values. Perhaps the colleges are not overly ethical at times in bartering too low for highly qualified engineering teachers. In our perplexing problems of personnel, of which we have had many, we found industry genuinely co-operative in its willingness to help. Many of the industrial leaders are farsighted in such matters. They envision that their research and design engineers of tomorrow, their future executives as well, are the offspring, let us say, of university adventures in the higher technical educations of today. In consequence, they want these prospective employees taught with utmost thoroughness. They would prefer the colleges of engineering to administer the basic instruction, but because they have not found the graduates well grounded in basic science and engineering, a number of the larger companies have felt impelled to do the job themselves concurrently with their inductive training for specialized branches of service.

As though to lend support and credence to this implied malignment of the incompetence of instruction, how often do we see the graduate instructor who is, figuratively, but a hop, skip, and jump ahead of his class—the instructor who can lay claim to no achievements in his sphere of technical activity? He is restricted in his services by a lack of knowledge and facility with mathematics; he is circumscribed by a "low ceiling" of vision in his specialized field; and he has literally a complete "blackout" of knowledge in the general field of engineering and science. How can the instructor who has achieved no success at the levels of graduate endeavor, lead, inspire, or encourage his students to the attainment of what he, himself, has been unable to attain?

Figure 1. The organization chart for the electrical-engineering department, Polytechnic Institute of Brooklyn, showing the lines of delegated responsibilities


DEPARTMENT ADMINISTRATOR
DEPARTMENT OFFICE
GRADUATE — RESEARCH DIVISION
POWER DIVISION
COMMUNICATIONS DIVISION
DEPARTMENT MACHINE SHOP
RADIO CONSTRUCTION SHOP
GRADUATE — RESEARCH LABORATORY
ELECTRICAL MACHINERY LABORATORY
COMMUNICATIONS LABORATORY
GRADUATE COURSE PROGRAM EVENING
UNDERGRADUATE COURSE PROGRAM DAY — EVENING

The Administrator

The administrator of an active, well-organized department finds himself with a full-time job of attending to its manifold activities. His services to the college and to his department divide themselves into two major lines of responsibility. On the one hand, the dispatch of the minutiae of normal routine—in trying to keep the multiple divisions of the department's activities progressing smoothly and effectively—constitutes his most time-consuming, but lesser important, function, while on the other hand, his major administration, and the one by which his competence is primarily judged, is his leadership in engineering education. Does he promote high standards of instruction; does he stimulate in the students and faculty members inspiring ambitions; and does he encourage in his associates the joy of planning for future developments and achievements?

In the execution of his duties he has probably discovered by experience how much more satisfaction is derived through the sharing of certain groups of these responsibilities with the fellow members of his staff. At the Polytechnic Institute, this plan has been effectuated in the electrical engineering department through the organization of three divisions—the power division, the communications division, and the graduate and research division. The organization and functional chart which shows the lines along which the department has developed over a period of years is portrayed in Figure 1 to indicate the major divisions of delegated responsibility. In each of these, further delegations of duties are carried down even to the grade of assistants and graduate fellows, so that everyone on the staff has his part in the administration of duties commensurate with his position and in addition to his major function of service.

In this paper we are primarily concerned with the organization of the graduate and research division which coordinates and administers all matters pertaining to the evening graduate courses and to programs of research. The organization chart for this division is shown in Figure 2 to illustrate the lines of responsibilities here. Under this plan each member of the staff is charged with definite duties of a restricted scope which he is encouraged to administer within a certain degree of autonomy. His efforts and accomplishments are given full recognition, and his initiative and enterprise are factors which are considered in his advancement. Ideas and plans for improvements in the service of the division are discussed in meetings of its personnel, and when necessary department meetings are held to discuss the broader aspects of those plans and proposals wherein the composite judgment of the staff members is desired.

The department head co-operates to his fullest extent in trying to relieve the staff members of the graduate division from any arduous duties of administration that will unduly distract their attentions from their graduate and research endeavors. He should recognize that needless distractions from their absorbing problems demoralize their interests and dissipate their efforts. Among the administrative problems which he may find at times almost insurmountable, and which have their origin in just such distractions and interruptions, are those of providing required space facilities, of acquiring expensive pieces of necessary specialized apparatus, and of securing adequate and capable mechanician services and laboratory assistants. These problems arise largely, of course, because of inadequate appropriations—a subject mentioned with respect and without comment.

A word of caution which the administrator should impart to all instructors of evening graduate courses is that of urging them to so allocate their time as to provide unfailingly for adequate preparation of all phases of their course notes. The lure of research must not be allowed to unbalance their sense of proportion. Unpreparedness for a session is no less disastrous to the success of the course than is incompetence. The young men of industry who enroll for graduate study are making sacrifices of their marginal time, and, too, they are paying well for the privilege. They have confidence in the integrity of their neighboring engineering college to provide instruction of the highest quality. The administrator of the courses should make doubly certain that he is not inadvertently selling to these ambitious young men "gold bricks" of education.

Table I. Showing the Groups of Courses in the Graduate Program of Electrical Engineering Leading to the Master's and Doctor's Degrees, Polytechnic Institute of Brooklyn

G619	Introduction to the Theory of Functions	(M)
G351	Analysis of Transient Phenomena (Pre G619)	(M)
G625	Vector Analysis	(M)
G353	Electromagnetic Theory (Pre G625)	(M)
G357	Theory of Electrical Measurements*	
	Major Course Groups	
	Power	
G361	Power Transmission and Distribution Theory	
G363	Advanced Alternating-Current Machinery*	
G367	Design of Electrical Machines*	
EEW-254	Protective Relaying of Power Systems*	
G731	Dynamics of Machines and Vibrations*	
G740	Advanced Thermodynamics	
G741	Steam Power Plants*	
G743	Steam and Gas Turbines*	
	Communications	
G371	Theory of Electronic Tubes and Their Circuits	
EEW-293	Design and Application of Electronic Tubes*	
G373	Transmission Network and Filter Theory*	
G377	Advanced Network Theory* (Pre G619, G373, G627, G351)	
G384	Ultra-High Frequency Theory* (Pre G353)	
G355	Conformal Mapping* (Pre G619)	
	Electrophysics	
G837	Introduction to Theoretical Physics	
G627	Fundamentals of Mechanics*	(D)
G845	Statistical Mechanics*	(D)
G381	Fundamentals of Electronics*	
G841	Fundamentals of Radiation*	
G847	Quantum Mechanics** (Pre G845)	
G629	Higher Mathematical Analysis II*	
	Minor Course Groups	
	Physics	
In addition to "Electrophysics"		
G831	Advanced General Physics	
G834	Acoustics**	
G835	Optics**	
G842-3	Physical Measurements	
G844	Conduction of Electricity Through Gases**	
	Mathematics	
In Addition to "Basic Course Group"		
G621	Advanced Calculus	
G623	Differential Equations	(D)
G629	Higher Mathematical Analysis I**	
G631	Matrix and Tensor Analysis*	
	Physical Chemistry	
123	Elementary Physical Chemistry	
G121	Advanced Physical Chemistry	
G123-4	Advanced Physical Chemistry Laboratory	
G125	Chemistry of Colloids*	
G126	Chemical Thermodynamics*	
G127	Advanced Electrochemistry*	
G673	Metallography and Heat Treatment*	
G692	Metallurgical Thermodynamics*	

(Pre) Prerequisite courses.
(M) Courses required for the Master's Degree.
(D) Courses required additionally for the Doctor's Degree.

* Given in alternate years.

** Given every three years.

In the graduate instruction of these young men, almost the entire gamut of engineering practice may be represented in the experiences of the combined membership of the class, so that questions of a wide variety, and of astounding degrees of depth, may enter the discussions of even the first-class session. It is by no means uncommon to have numbered among the class members, those with an enviable array of technical publications pronouncing them authorities in their particular fields of endeavor.

How obvious it appears, then, that these young men should expect in their mentor one thoroughly versed in his field in order that he should stimulate them to their greatest attainments, and inspire them to carry on far beyond the confines of the class lessons. Let the administrator spend more time and more resources to provide his department with a staff of research investigators and graduate teachers who will meet the fullest expectations of these young men.

The problem which confronted us at the Polytechnic Institute in the beginning of this new venture, and one which may perplex others who are contemplating the organization of this form of education, or its counterpart in nonresident instruction, was how to provide this highly qualified instructing personnel. In those early days we were feeling our way along through the shoals of both economic and educational hazards. The "pay as you go" policy which we adopted from the start certainly did not make easy the problems of providing graduate instructors. We solved this problem temporarily by affiliating with us some of the foremost scientists, engineers, and industrial research specialists here in the metropolitan New York area.

We induced a few of these specialists who were especially well qualified through the breadth of their educational background and of their experience, through their love of their work, and through their demonstrated ability to impart their knowledge to others, to join, as part-time members, our graduate staff. They organized and offered instruction in certain prescribed basic courses in which they were pre-eminently qualified to bring to the class a wealth of experience in industrial applications, a broad vision of the frontier problems of present day science, and a richness of enthusiasm for their subjects. By this means we were enabled to establish and maintain high standards in regard to the qualifications of our teaching personnel.

The department was also privileged to have associated with it, during the formative period of this venture, the part-time services for two years of Vladimir Karapetoff, the revered dean of America's electrical engineering teachers, and the full-time services for one year of Bernard Hague of the University of Glasgow, the pre-eminent authority on electrical measurements, as a visiting professor.

The Curriculum—at the Master's Level

The development of a curriculum of courses leading to the master's degree and to the doctor's degree is a subject charged and surcharged with controversy. The adherents of the "free choice" of subjects at the graduate level would probably not condone the curriculum that contains a large percentage of required sub-

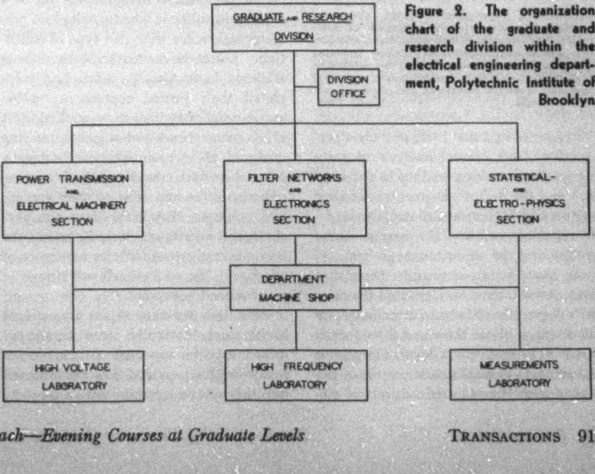

GRADUATE AND RESEARCH DIVISION
DIVISION OFFICE
POWER TRANSMISSION AND ELECTRICAL MACHINERY SECTION
FILTER NETWORKS AND ELECTRONICS SECTION
STATISTICAL- AND ELECTRO-PHYSICS SECTION
DEPARTMENT MACHINE SHOP
HIGH VOLTAGE LABORATORY
HIGH FREQUENCY LABORATORY
MEASUREMENTS LABORATORY

Figure 2. The organization chart of the graduate and research division within the electrical engineering department, Polytechnic Institute of Brooklyn

jects. After lengthy consideration of this important subject, and based upon several years of experience, we are the more convinced that, in the curriculum leading to the master's degree, certain subjects are so basically important that the omission of any one would seriously weaken the foundation upon which the superstructure of the student's subsequent educational endeavors is based.

The subjects which we require of all electrical engineering students are:

1. Vector analysis.
2. Introduction to the theory of functions.
3. Analysis of transient phenomena.
4. Electromagnetic theory.
5. Electrical engineering seminar.
6. Thesis

These courses make a total of twenty semester hours of the minimum of thirty which are required for the degree. The elective subjects, totaling ten or more semester hours, are chosen by the student in conference with the department adviser, and these subjects are generally more or less closely allied to the field in which the student is employed during his day hours.

These lines of specialized study have been formulated into three general "major" classifications in each of which the following subjects are included:

Power Engineering. Electric-power generation, transmission and distribution, system calculations, electrical protection, system stability, design of machines, transient behavior of machines, and a selection from several subjects in mechanical engineering.

Communication Engineering. Electronic tubes, their theory and their circuits, network and filter theory, advanced network theory, fundamentals of electronics, ultrahigh-frequency theory, conformal mapping, theory of electrical measurements, fundamentals of mechanics, and a selection from several basic subjects of physics, including acoustics, introduction to theoretical physics, and fundamentals of radiation.

Electrophysics. Conformal mapping, theory of electrical measurements, fundamentals of electronics, fundamentals of mechanics, and a selection from several basic subjects of physics, including fundamentals of radiation, physical measurements, conduction of electricity through gases, statistical mechanics, quantum mechanics, and physical properties of metals.

Reference to Table I will provide a perspective of our present program of evening graduate courses leading to the master's and the doctor's degrees in electrical engineering. Here are shown the basic, or required, courses, the major course groups, and the minor course groups.

An analysis of our records shows that students who have been granted the master's degrees have been in attendance, on the average, about three and three-fourth years. The first year is devoted largely to the subjects of mathematics, while in the second year the required subjects of circuit analysis and electromagnetic theory are taken; during the third year the student generally completes the elective subjects and starts thesis work, and in the fourth year his time is wholly devoted to the thesis investigation. Occasionally students with less call on their marginal time by outside activities, or home ties, take an extra course or two in their first two years—thereby sometimes completing their thesis work and all other requirements for the degree at the end of the third year. Attendance without credit is required at the electrical engineering seminar during each year of enrollment in electrical courses. The speakers at these seminar sessions, who are invited to address the students and staff members and lead the discussion, are outstanding men of science and engineering here in the New York area.

The Thesis

Of the many aims and objectives in graduate study, the outstanding one is, in a few words, the development in the student of his capabilities to carry on a coordinated program of original thinking and independent investigation. We believe that the thesis is an essential means of appraising the student's ability to conduct an advanced and original investigation of an appropriate engineering or scientific subject, of showing his mastery of the subject matter of the courses he has taken, of demonstrating the development of his processes of clear analytical thinking, and of proving his facility in expressing his thoughts and his developments of theoretical conceptions, properly and adequately, in technical language.

The competence of these evening graduate students to undertake and vigorously attack their thesis assignments will be a revelation to those who have had no previous experience with this type of education. Let it be remembered that these students have already taken and completed their formal courses in mathematics, and that they have applied most of it many times subsequently in the study of their basic electrical subjects. After they have completed all of these required courses and most of their specialized subjects, they then have the psychological advantage of undertaking the thesis investigation with an assurance of mind and the educational advantage of an advanced viewpoint.

With this training as an educational background, naturally these thesis enrollees may be expected to achieve superior results, provided they are inducted into this new undertaking with a personal understanding of their possible pitfalls and of their inhibitions to use all they have learned to the best ends. The assignment and guidance of thesis work requires a particularly careful vision as to the probable length and character of the problems, the facilities for laboratory measurements, and the general nature of the theoretical aspects of the problems. It is so easy to extend beyond the bounds of a reasonable thesis, unless proper care is exercised.

Not infrequently students are enabled to carry on their investigational work under the co-operative auspices of their supervisors on their jobs, in which cases a close collaboration is maintained between an assigned member of the graduate division of the department and the direct supervisor in the company with which the graduate student is employed. This co-operative plan for the thesis investigation is especially helpful in certain projects that involve a wealth of specialized apparatus with which the company may be well equipped.

We have been operating under another co-operative plan for the past three years by which employees of the Westinghouse Electric and Manufacturing Company may receive graduate course credits, to a maximum of fourteen, towards the master's degree at the Polytechnic Institute for courses which they have passed with honor grades in the company's graduate program. Reciprocally, our students may elect to take certain courses, for credit, offered by company specialists on the Westinghouse plan. These courses for interchange credit are restricted to the applied types, in particular. New York University and Stevens Institute of Technology also share in this Westinghouse plan in the New York area.

The Doctorate Program

Having organized and administered for several years the curriculum leading to the master's degree, an insistent demand kept growing for the extension of our graduate program into the doctorate level. Several of those, who had made meritorious achievements in their work leading to the acquirement of the master's degree, were engaged in the research activities of various neighboring industries. Most of them had already published papers setting forth the results of these various research investigations, but they wished additional knowledge in the collateral fields of electrochemistry, electrophysics, and mathematics, and in the frontier science of electrical engineering. For their immediate benefit, a number of these young men continued their studies in the various advanced courses which were being offered in mathematics, physics, and electrical engineering, although they expressed their hope that eventually the electrical engineering department would offer a program of study leading to the doctor's degree. This step was formally taken in 1936.

In organizing instruction at this level, we departed basically from our objectives and philosophies underlying the master's program. Here, we held that "freedom" should be allowed the students in choosing broadly from those courses which would focus their attentions most specifically upon the lines of study in which their life's work seemed directed. To assure definiteness of purpose, it seemed desirable that these subjects be classified within a major field and within one, or possibly two, minor fields of concentration, and at least one of the minors be chosen outside of the offerings within the major field. Students who enroll for doctorate study are referred to individual guidance committees whose purposes it is to direct their selection of courses of study and collateral reading, and to advise them in the preparation for qualifying examinations in the subjects of their major and minor fields, in gaining an adequate understanding of two foreign languages, and in undertaking the investigational program that constitutes the dissertation.

Thus far we have graduated three students with the doctor of electrical engineering degree, but an increasing number are aligning themselves for qualification in the near future. The prospects look favorable for the granting of three, or possibly four, doctor's degrees at the end of the current academic year. Actually, about twenty evening students are at various stages of doctoral preparation above the master's level at present. The number of course credits above the master's degree leading to the doctorate is a minimum of twenty-four and the time required for the dissertation is the equivalent of at least sixteen additional credits. The selection of students for this level of educational attainment is rigorously based upon the evidence of ability which they have displayed in prior years in achieving honor grades for courses at the master's level, and in conducting meritorious research work. Students are not allowed to enroll in the doctorate program unless they have conducted thesis investigations for the master's degree.

It is too early in the progress of our venture in this division of graduate service to draw conclusions. Yet from our experiences in the instruction of those who have already obtained the doctor's degree and of the twenty now enrolled in this advanced program, we believe that we are headed in the right direction. If such is possible, our applied philosophy is one of overemphasis on thoroughness, high quality, depth of specialized study, and unquestioned ability to carry on independent studies and investigations.

This process of education, through evening study, imposes an unrelenting and self-administering firmness in maintaining the quality of its attainments by discouraging those who cannot stand its rigors. Those possesssing lesser qualifications, who, in consequence, must devote abnormal time to the preparation of their assignments, or who just cannot orient themselves to originality of thought are self-prompted to withdraw voluntarily. At this level of education, the students are not given the benefit of doubt in the event of their work falling slightly below honor grade. We believe that at the doctorate level the time for the "mollycoddling" of students has definitely passed. They are expected to perform at high levels of excellence in all of their work and at all times.

Decentralized, or Nonresident, Instruction

The basic elements in the organization of nonresident graduate evening courses which must be given careful consideration by those who contemplate embarking upon this type of educational enterprise are

1. The choice of subject to be offered.
2. The selection of a well qualified instructor.
3. The development of the content of the course.
4. The establishment of prerequisite courses, especially in mathematics.
5. The procurement of suitable housing facilities.
6. The qualifying of applicants.
7. The provision for critical supervision.

The ultimate objectives of this decentralized instruction determine to a large degree the specific emphasis of those policies which influence the decisions on some of the above elements. For example, is it contemplated that the courses are to constitute a miscellany of specialized subjects in electrical engineering to provide advanced instruction for employees in industry who seek immediate answers to their urgent problems of design and manufacture? If so, are the courses to be organized at a truly graduate level, with prerequisite courses made available, and with admission allowed only to qualified applicants? In this case, graduate credit for the courses could be allowed by the college, although this is an emolument of doubtful value unless a full program of nonresident courses is planned whereby the applicants may be encouraged to continue for the master's degree.

On the other hand, perhaps the contemplated courses are to form a co-ordinated program of basic and applied subjects in science and engineering which will provide the equivalent of the day instruction leading to the master's degree that may already be in operation at the college. The latter plan, of course, encompasses an elaborate duplication of facilities at the urban center which now constitutes a part of the campus organization. In effect, this plan anticipates the establishment of another division of the department of electrical engineering, which is specifically organized to offer graduate evening instruction within the confines of the neighboring urban area. A plan of this magnitude is, of course, a major undertaking and one which should be subjected to most careful economic analysis and critical study.

The former plan of offering a few specialized engineering courses at graduate level, either with or without college credit, may be readily initiated, and the present time seems to be particularly favorable for starting them. Such courses could be organized to meet the current demands of industry for intensified technical specialization by providing instruction for those employees who find themselves inadequately prepared to cope effectively with the exacting problems of this critical period. This plan, if effectuated now, would possess the dual advantages of meeting the present training needs of a war-time industry in its present emergency and of being organized at a most favorable time to encourage its financial support. With the present dearth of personnel possessing specialized training which now exists in many quarters, financial aid could probably be obtained from the government, but if not, where the need really justifies it, neighboring industries could, most likely, be induced to sponsor the courses and to subsidize small deficits of operation.

The Polytechnic Institute has offered some instruction, by request, on a nonresident basis, wherein a considerable number of employees at a distant plant wished a specific course. In these cases it was possible for a member of our normal staff to conduct the course. Graduate credit was allowed only to those who fulfilled our normal qualifying requirements. We are now finding a most urgent de-

Resistance-Welding Transients

E. E. KIMBERLY
MEMBER AIEE

Synopsis: This paper discusses quantitatively the effect of indiscriminate (random phase-angle) switching in the primary of a resistance-welding transformer supplying a resistance welder. The current and power transients occurring because of "off-angle" switching are investigated through oscillographic records of transients in loads built up to simulate actual welder-head loads as nearly as possible.

In the switching of loads of either high or low power factor and those with or without iron at high-flux densities, the power transients were found to be damped to unimportant values within the time of a very few cycles after the instant of switching. In spot and projection welding, the first few cycles may be extremely important, however, determining the quality of weld even though the total time of the weld period is much greater. This investigation is limited to 60-cycle frequency and to welding periods of two cycles or more.

The General Problem

When a circuit containing resistance and reactance is energized from a source of alternating current, its initial current is likely to be greater than it will be after a few cycles have elapsed.[1,2] The current is also likely to be affected by the momentary unusual resistance voltage drop in the primary winding of the supply transformer. Inasmuch as this paper applies only to heat transients in the weld itself, the surge of current in the supply transformer primary will be considered only as it affects the heat produced in the weld because of its effect on the secondary voltage. The transformer used had an impedance of about 10 per cent.

Because of the great diversity of types of resistance-welding loads in which both ferrous and nonferrous metals are placed in the welding throat, generalization is somewhat difficult, and only a limited number of specific cases can be considered in detail in a paper of this kind. All resistance welders have a common characteristic, however, in that their welding heads carry heavy currents, and the leads to the transformer secondary coil enclose an area which accommodates a flux which produces an inductive reactance of some importance. Whether the metal introduced into the throat be ferrous or nonferrous, the welder head and its transformer leads can be considered theoretically and practically for transient analysis the same as any other circuit consisting of resistance and inductive reactance in series. This investigation was carried out using three types of artificial circuits built up to simulate representative welding loads.

Circuit 1 was of unusually high power factor of 0.85. Circuit 2 was of medium power factor of 0.5. Circuit 3 was of more common power factor of 0.22. The power factor of a welding circuit is, of course, fixed by the ratio of resistance to the reactance caused by the magnetic flux enclosed by the throat. From this fact it is apparent that a large throat or ferrous metal in the throat will be conducive to low power factor. With the shortest possible throat depth in a practical design it is difficult to obtain a power factor when spot welding of more than about 0.6 with a frequency of 60 cycles.

No attempt was made to control the residual flux of either the transformer or of the load, because it was considered reasonable to assume that the stock to be welded has no remanent magnetism and that the transformer residual flux will produce in these tests the same secondary-circuit effects as in an ordinary welder transformer. The welding current used was about two times the permissible continuous current in the transformer.

Method of Testing

To examine the power transient characteristics of the three representative circuits, they were switched on at a great variety of instants on the applied voltage cycle, and many visual observations were made. Only those found to indicate special conditions, such as maximum additive or subtractive power transients were repeated, photographed, and reproduced in this paper. The curve sheets, therefore, show extreme results of "off-angle" switching for the various circuit conditions. The time constant of flux decay as expressed by the ratio of inductance to resistance, L/R, is affected not only by the resistance of the welder and transformer secondary coil and the flux enclosed by the throat but also by the entrapped flux in the transformer core itself. Because of this fact, the rate of decay of transient current is influenced by the size of transformer used relative to the welder requirements; the rate of decay being less as the transformer size is increased.

The energy delivered to the weld for any period was found by planimeter integration of the power curves on the oscillogram. The power oscillograms were taken by a power galvanometer with the current coil in series with the welding circuit and the potential coil connected across the secondary of the welding transformer. While the power transient had not entirely subsided in some cases by the time the end of the oscillogram was reached, the steady-state energy per cycle was available in every case from another oscillogram in the same series with the same load.

Results and Conclusions

1. Random switching may result in a good weld, a cold weld, or a hot weld when the welding period is less than about 20 cycles.
2. Random switching may cause as much

Paper 42-25, recommended by the AIEE committee on electric welding, for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted May 8, 1941; made available for printing November 25, 1941.

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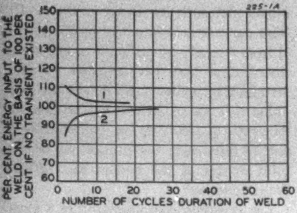
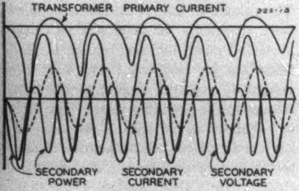
mand from neighboring industries for instruction in the theory of ultrashort waves, their production, transmission, and reception. This course was included in our current curriculum for the second semester, and we are advised to expect a large enrollment. In fact, we have been requested to consider the organization of certain classes in the subject at remotely located plants where a sufficient number might warrant nonresident instruction.

The call of the junior engineers of industry for more and more of this type of graduate evening instruction offers a resounding challenge to the technical schools, in or near the urban centers, to organize their facilities for taking an active part in this growing enterprise. Already a number of colleges of engineering throughout the United States has responded to the opportunities offered in this attractive field, and others are now formulating their plans to inaugurate programs of advanced courses in the near future. These colleges will soon find, too, that the fruits of their new adventures bear to them deep satisfaction and stimulating pride in the achievement of their discovery of a new and broader field of service.

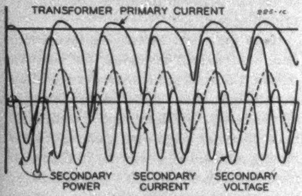


50 per cent power factor

1. Transformer primary closed at zero line voltage
2. Transformer primary closed 45 degrees before zero of line voltage

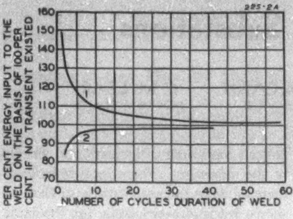


Oscillogram 1



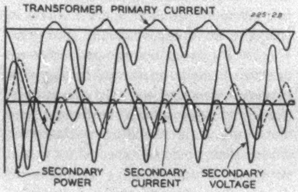
Oscillogram 2

Figure 1. Oscillograms and heat-input curves for circuit 1

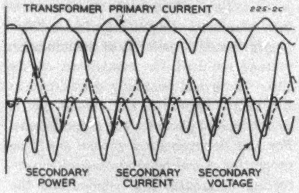


85 per cent power factor

1. Transformer primary closed at zero line voltage
2. Transformer primary closed 68 degrees before zero of primary voltage

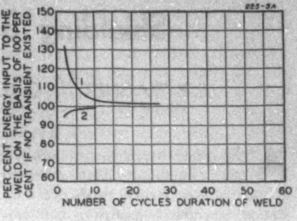


Oscillogram 1



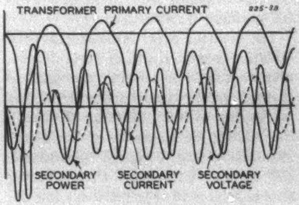
Oscillogram 2

Figure 2. Oscillograms and heat-input curves for circuit 2

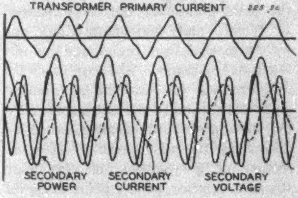


22 per cent power factor

1. Transformer primary closed at zero line voltage
2. Transformer primary closed 85 degrees after zero of line voltage



Oscillogram 1



Oscillogram 2

Figure 3. Oscillograms and heat-input curves for circuit 3

as 50 per cent variation in the heat produced in a weld having an "on" time as short as two cycles, because of unpredictable transients. Such transients may also soften the electrodes and materially shorten their useful lives.

3. Random switching will cause an error not in excess of five per cent in the heat produced in a weld having an "on" time exceeding 20 cycles at a line frequency of 60 cycles. This statement is true only on condition that the welding circuit is opened between welds, or that the time interval between consecutive welds is sufficient to permit the complete dying out of entrapped flux in the throat. Any entrapped flux lingering from a previous weld period will tend to be accumulative in further saturating the transformer core.

In the case of projection welding even when the weld time does exceed 20 seconds, a transient of added energy in the first two or three cycles may be disastrous, because of the exploding away of the projections, and for that reason accurate switching control is imperative.

4. When the welding period is less than about ten cycles the heat delivered to the weld may be appreciably increased or decreased at will within small limits, depending upon the number of cycles of the period by controlling the instant of closing the primary circuit of the transformer. This procedure is permissible, of course, only if the voltage disturbance caused by the transformer primary-current inrush is not excessive when the switching point on the voltage wave departs from that at which the minimum transient occurs. Such practice may, however, result in the softening and extrusion of the electrodes.

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Shielding of Substations

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As compared to transmission lines, it is more important that overhead ground wires or vertical masts over substations be correctly located so as to provide shielding of the structure against direct strokes of lightning. In a previously published paper[1] the authors discussed the shielding characteristics required for transmission lines. The present paper extends these investigations to the shielding of substations.

The previous paper indicated that the essential characteristics of natural lightning, that must be correctly simulated so that laboratory sparks and scale models can be used to study shielding effects, are the relative development of the initial streamers of the discharge. Schonland[2] and his associates found that lightning strokes to ground or to relatively low objects, such as transmission line towers, are initiated by a streamer propagating from the cloud practically the total distance to ground. Only very short, if any, upward streamers from the ground end are found to be present. The path taken by a stroke and its resulting terminating point on the earthed end is determined by the initial downward streamer, called by Schonland, the pilot streamer. The direction of propagation of the pilot streamer depends, at any point along its path, upon the electric field produced by the charges in the cloud, at the ground, and in the streamer itself, and upon localized conditions of ionization at the tip of the streamer. These localized effects tend to make the path erratic so that, as shown by laboratory tests, for the same configuration of cloud and ground, no two strokes will follow the same path.

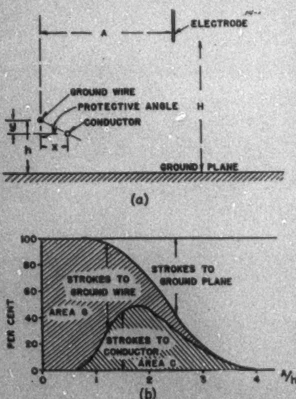

Figure 1. Symbols utilized with stroke-distribution curves for overhead ground wires

It was found that, although natural lightning is predominantly negative in polarity, the relative streamer development is best represented in the laboratory by positive polarity sparks, that is, strokes from a positive cloud. For negative laboratory sparks, upward streamers are more likely to originate from the most exposed object and span the greater portion of the gap spacing, thus giving rise to more optimistic shielding results with models than would be expected for actual strokes in nature.

Description of Model Tests

As in the previous work, this investigation employed $1^1/_2$x40-microsecond impulses of positive polarity at the minimum voltage required for breakdown of the gap. A vertical pointed rod was used as the cloud source of the stroke and a smooth metal plane for the ground plane. For the determination of the shielding properties of overhead ground wires protecting horizontal line conductors, the arrangement shown in Figure 1a was used, in which the symbols are clearly defined. Owing to the localized variations at the tip of the pilot streamer, all strokes from a given position of the cloud electrode do not follow the same path and terminate at the same point at the ground. Thus, the strokes divide between the three possible terminating points: the ground wire, the conductor, and the ground plane. It is found that the probability of the protected object being struck although decreasing as more favorable protection is afforded does not necessarily become zero.

Because the shielding characteristics are the same at any point along the line, they can be determined from a two-dimensional plot such as shown in Figure 1b. This curve was determined by increasing *A* from zero and counting the proportional distribution between the three possible terminating points until *A* reaches such value that all strokes strike the ground. The ratio of area *C* to the sum of areas *C* and *G* represents the proportion of the total strokes to the system that strikes the conductor.

The shielding characteristics of a mast involve a three-dimensional problem, as shown in Figure 2a, which defines the symbols used. The origin of the stroke is located by the dimensions *A* and θ, and the distribution curves now become the three-dimensional surfaces of Figure 2b. The volume *C*, that resembles a rounded cone, represents the total strokes to the protected mast and the volume *G*, that resembles an inverted basin with the volume *C* removed, represents the total strokes to the shielding mast. The ratio of volume *C* to the sum of the two volumes is the proportion of the total strokes to the system that strikes the protected object. For a given position of the cloud electrode, 50 strokes were found sufficient to determine the percentage distribution of strokes between the three possible terminating points.

Since the effect of varying model size had been found[1] negligible for positive polarity, a fixed ground wire or shielding mast height, *h*, of 10 inches was used for all the tests. To determine the effect of mast diameter, tests were made with rods having rounded tips varying in diameter from $^1/_{16}$ inch to $^1/_4$ inch. Substantially, the same results were obtained, regard-

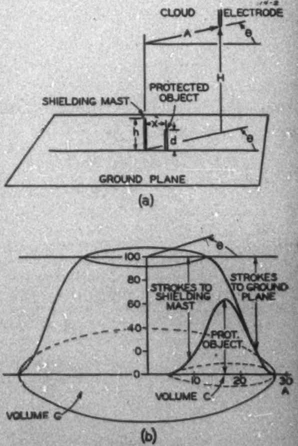

Figure 2. Symbols used with stroke-distribution curves for vertical masts

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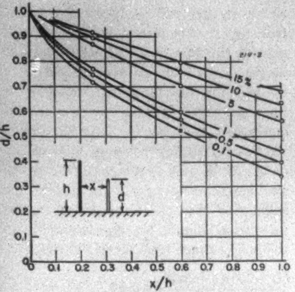


Figure 3. Exposure of an object protected by a single mast

less of the size or combination of rods, as long as positive polarity was used, and the rod sizes were held within this range. From this it can be concluded that the data obtained with one rod size are applicable to practical construction. For subsequent tests $1/8$-inch rods were used. Previous tests[1] showed that the same conclusions apply to horizontal ground wires and conductors.

To determine the total effect of cloud height, the relative frequency with which strokes originate from a given height should be known. Sufficient data of this type are not available and considerable variance undoubtedly exists in different regions. It is known that the base of thunderclouds[1] varies in height from a minimum of about 500 feet above ground to as high as 20,000 or 30,000 feet. A common minimum for relatively flat terrain is about 1,000 feet. Since more pessimistic results are obtained with lower cloud heights and ratios of H/h, minimum values should be used. A ratio of H/h of 5 was taken for this study, which for a 200-foot mast results in a 1,000-foot cloud height and for a 100-foot mast in a 500-foot height. The use of this ratio should give conservative results.

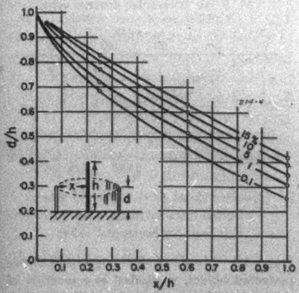


Figure 4. Exposure of a ring of objects protected by a single mast

Shielding of One Mast by Another

In Figure 3 are plotted the results of the tests made with the configuration of masts shown in the insert. The strokes that contact the protected mast, expressed as a percentage of the strokes to the system of masts, are plotted as a function of the ratio of d/h and x/h.

The actual configuration of the equipment to be protected, such as a substation bus structure, may vary widely and it would be very difficult to make a study of all configurations. However, it will be shown that a few fundamental configurations, such as the one discussed, are sufficient for practical purposes. The performance of one mast protecting another is applicable to the case for which a single mast protects a structure having a single prominent projection.

Shielding of a Ring of Masts by One Mast

If a number of points on a structure have equal exposure with respect to a single shielding mast, the probability of at least one being struck is increased. This can be seen with reference to Figure 2b. If there were another mast of the same height, d, and distance, x, from the shielding mast but at an angle θ equal to 180 degrees, there would be then another volume C, and the number of strokes to the two protected masts would be twice that to one. As the number of masts increases, forming a circular ring around the shielding mast, the exposure increases until the limit of an infinite number of such masts or a solid ring is reached. The distribution curves for such a case are independent of θ. A conservative estimate of the shielding performance of such ring can be obtained by assuming that these distribution curves are the same as that of a single protected mast for θ equal to zero.

Thus, the volume C is a single volume of revolution about the shielding mast whose cross section is the heavy curve of Figure 2b. This is somewhat conservative, because, as the number of masts in the ring is increased, they eventually become so close that more than one mast becomes involved in the distribution curve and less than the number indicated by the curve for one mast for θ equal to zero will strike any one of the masts.

It was in this manner that the data for the curves of Figure 4 were obtained. These data are directly applicable to a structure having all points of equal height and equal distance from the shielding mast. Almost all practical cases of shielding by means of a single mast will lie between this condition and that of one protected point.

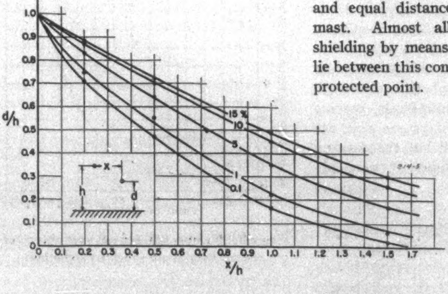


Figure 5. Exposure of two horizontal conductors protected by a single overhead ground wire

Table I. Record of the Number of Times Objects of Varying Heights Are Struck

Object and Location	Height (Feet)	Number of Years	Times Struck	Average (Number Per Year)
Mast at North Wales substation (Philadelphia) of Philadelphia Electric Company	80	4	1	0.25
10 fire towers of the Pennsylvania State Department of Forests and Waters, western Pennsylvania	100	1	2	0.2
Radio tower of WWSW, Pittsburgh	100	3	1	0.33
Radio tower of WHK, Cleveland	300	1	1	1.0
Radio tower of WCLE, Cleveland	300	1	0	0
Radio tower of WADC, Akron	360	3	6	2.0
Cathedral of Learning of University of Pittsburgh	535	3	8	2.7
Anaconda Copper Mining Company smoke stack at Great Falls, Mont.	545	2	1	0.5
Anaconda Copper Mining Company smoke stack at Anaconda, Mont.	565	2	5	2.5
Empire State Building, New York, N. Y.	1,250	3	68	23

These objects are in regions of isoceraunic levels varying from 25 to 45 storm days per year.

Shielding of Two Horizontal Conductors by a Single Overhead Ground Wire

The shielding characteristics of two horizontal conductors protected by a single overhead horizontal conductor are given in Figure 5. These curves can be applied to such cases as an overhead wire shielding a substation or shielding the incoming lines to a substation.

Shielding of Objects Between Two Masts or Ground Wires

Data similar to that given above are presented in Figures 6 and 7 for a protected mast located midway between two shielding masts and for a single horizontal conductor located midway between two parallel overhead ground wires. The increase in shielding, which is obtained when several masts or ground wires are used and placed such that they more or less surround the protected equipment, is not generally realized. The area protected by two masts or two ground wires is considerably greater than twice the area protected by one.

Total Number of Strokes to Substations

The degree of shielding necessary for adequate protection can only be determined after it is known how frequently the system is struck. Data available to the authors on the number of times per year objects of varying heights are struck in regions of isoceraunic levels, varying from 25 to 45 storm days per year, are listed in Table I. All but the data on the Empire State Building[3] were obtained from lightning investigations being conducted by the Westinghouse Electric and Manufacturing Company.[4]

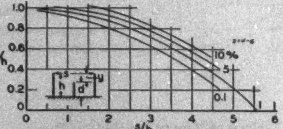


Figure 6. Exposure of a single mast midway between two shielding masts

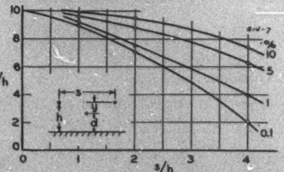


Figure 7. Exposure of a single conductor midway between two ground wires

The curve of Figure 8 was obtained by grouping the data of Table I into mean values of height and averaging the strokes per year for each group. The range of this curve as applied to substations, would fall below 200 feet. A mast of such height can be expected to be struck about once every one and one-half years, and a 100-foot mast about once every three years. Laboratory tests indicate that for strokes that do not have appreciable upward leaders, the strokes attracted to a mast increase linearly with the height of the mast. This relation is indicated by the general shape of the lower part of Figure 8. The upward trend of the curve for high objects is probably due to the upward streamers that occur in nature from objects of such height.

A further estimate of the number of strokes to a substation can be obtained from the data of Waldorf[5] and others[1] on the frequency of strokes to transmission lines. The average figure for lines of from 60 to 100 feet in height is one per mile of line per year. The previous model tests[1] show that, in this height range, strokes will be drawn to the line from an effective lateral distance on each side of the line of about 3.5 times the height. Assuming an average height of 80 feet for the foregoing transmission lines, one stroke per line per year is thus equivalent to $5,280/(2\times3.5\times80)$ or 9.5 strokes per year per square mile of sky area. If W and L designate the width and length, respectively, in feet of the substation then the total strokes to the substation should be approximately, in the height range from 60 to 100 feet,

$$\frac{(W+700)(L+700)}{(5,280)^2}\,9.5$$

The strokes to a substation for which $W=L=100$ feet, are

$$\frac{(800)(800)}{(5,280)^2}\,9.5=0.22 \text{ per year}$$

or once every four and one-half years. This compares favorably with the data of Figure 8.

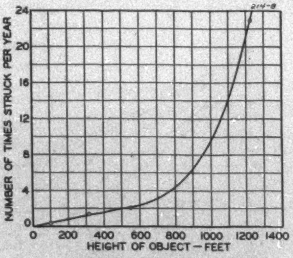


Figure 8. Number of times per year objects of various heights are struck by lightning

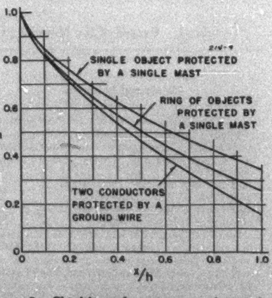
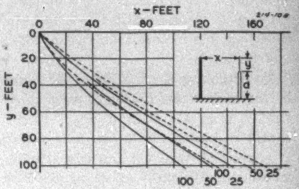
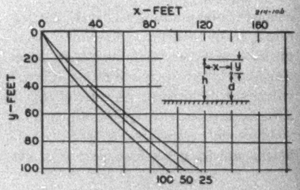


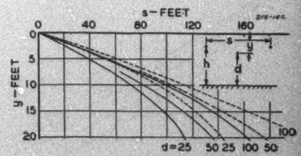
Figure 9. Shielding characteristics of a single mast or ground wire for 0.1 per cent exposure



(a) One shielding mast:
Dotted lines for one exposed object
Full lines for ring of exposed objects



(b) One horizontal ground wire



(c) Two masts or two ground wires:
Dotted lines for masts
Full lines for horizontal wires

Figure 10. Height of shielding object above protected object, y, plotted as a function of the horizontal separation, x, and the height of the protected object, d, for 0.1 per cent exposure

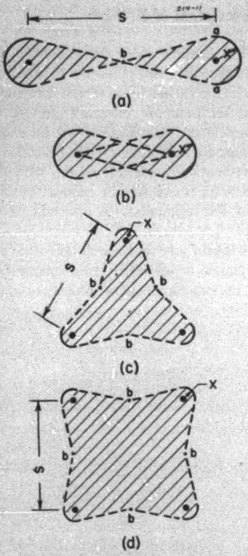


Figure 11. Areas protected by multiple masts for point exposures of 0.1 per cent

When considering a single substation and a figure of one stroke to the structure every two to four years, then, if an exposure of 10 per cent is assumed, the live parts will be subjected to one stroke every 20 to 40 years. One per cent exposure results in one stroke every 200 to 400 years, and 0.1 per cent exposure in one stroke every 2,000 to 4,000 years. However, many systems have a large number of substations, which increases the overall exposure.

Balanced against the desirability of perfect shielding must be considered the increase in cost incident to taller shielding structures. Certainly not over one per cent exposure should be permitted, and, when a comparison between the height of shielding structure required to obtain 0.1 per cent exposure over that for one per cent exposure, as obtained from Figures 3 to 7, is made, it will be seen that, in general, the added height can be obtained with little increase in cost. For this reason, the authors have chosen an exposure figure of 0.1 per cent in discussing the shielding of structures.

Working Curves

Figure 9 shows the relative configurations for a single mast or ground wire required to reduce the exposure to 0.1 per cent and Figures 10a and 10b show the same data in more usable form in which distances are plotted in feet directly, thus eliminating the necessity of using the ratios d/h and x/h. In these figures is introduced the distance y which represents the vertical distance between the protected object and the top of the shielding object. Similar data for two masts and two ground wires are presented in Figure 10c from data given in Figures 6 and 7.

Protection of Substations

To this point, consideration has been given to the protection afforded by one or two infinitely long wires, such as the overhead ground wires on transmission lines, to the protection afforded by one mast, and to the protection afforded to objects located at the mid-point of a line connecting two masts. Substation configurations are so diversified in construction that it becomes impossible to test each type individually. The best alternative is to convert the information already obtained to a form that can be utilized to best advantage.

A single mast protecting a substation offers no particular difficulty, the curves of Figure 10a being used directly. If the structure has a single prominent projection or several projections in a limited region, to be protected, such as a set of disconnects, the dotted curves should be used. On the other hand if live parts are more generally distributed at a given height, then the full-line curves should be used and applied to the most remote object.

For horizontal wires the data of Figure 10b apply to long spans, such as transmission lines. Substations involve much shorter lengths, some being so short that consideration must be given to the end effects. By comparing Figures 3 and 5 and Figures 6 and 7, for which Figures 3 and 6 approximate end conditions and Figures 5 and 7 apply to straight-away conditions, it may be seen that the per cent exposure for a given configuration is always slightly less for the end than for the straight-away Thus, the working curves, Figures 10b and 10c, can be applied directly to ground wires, even if they are short and the per cent exposure figures apply to the total strokes to the substation structure.

If two masts are used to protect an area, the data presented give shielding information only for the point b, midway between the two masts, and for points on the semicircles drawn about the masts as centers as shown in Figure 11a. For given values of d and y, a value of s from Figure 10c and x from Figure 10a can be determined, which will give an exposure of 0.1 per cent. The locus shown in Figure 11a, drawn by the semicircles around the masts as centers and connecting the point b, represents an approximate limit of 0.1 per cent exposure. Any single point falling within the cross-hatched area should have better protection than 0.1 per cent. This arrangement is likely to leave some points of a rectangular substation protected by two masts with higher exposure than desirable. If, however, the distance between the masts is decreased, the protected areas are, at least, as good as the combined areas obtained by superposing those of Figure 11a. For example, if the distance between masts is halved, the resultant protected area is somewhat as shown in Figure 11b.

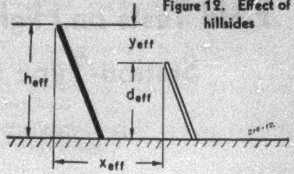


Figure 12. Effect of hillsides

On this basis, to form an approximate idea of the width of the overlap between masts, first obtain a value of y from Figure 10c corresponding to twice the actual distance between the masts. The width of overlap is then equal to x corresponding to this y as obtained from Figure 10a. This undoubtedly gives a conservative width of substation that can be protected.

For three masts located at the points of an equilateral triangle or for four masts located at the points of a square, the protected areas are as shown in Figures 11c and d. The height of the shielding mast should be so chosen that the b points provide 0.1 per cent exposure as obtained from Figure 10c for the mid-point between two masts. The x radii are obtained from the data for a single mast.

Effect of High Earth Resistivity and Terrain

The data presented here apply to stations located in regions of relatively flat terrain and low earth resistivity. As shown in the previous work,[1] high earth resistivity lowers the effective ground plane below the surface of the earth and results in poorer shielding for a given configuration. This effect is appreciable, however, only for very high values of resistivity, and, since most substations are provided with an extensive grounding

Field Tests on High-Capacity Air-Blast Station-Type Circuit Breakers

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IN January 1940 there was presented before the Institute a paper[1] describing a new high-capacity air-blast circuit breaker. Since then, breakers of higher interrupting rating have been built, following out the general principles of design and construction disclosed at that time. As part of an organized development program, such breakers have been subjected to extensive interrupting tests under factory laboratory conditions. However, it is recognized that the final proof of the interrupting performance of high-capacity circuit breakers comes as a result of tests made on actual operating systems.

The engineers of the Consolidated Edison Company offered to make such tests up to the full short-circuit capacity available on the bus of the Hell Gate Station in New York.

First Series of Tests

A breaker rated 15 kv, 1,200 amperes, 1,500,000-kva, 8 cycles, mounted in a steel cell, was submitted for field interrupting tests in September 1940. Out of seven tests, up to 1,480,000 kva, the breaker failed to clear on two occasions, resulting in a fault to ground inside the steel cell, which was cleared by the back-up protection. On the first of these, there was no damage whatever to the test breaker, and after the parts were inspected and the insulators cleaned, the tests were resumed. In the second case damage was limited to the upper barrier of the arc chute in the test breaker, and the glaze of some of the porcelain insulators. Adjacent equipment, such as the control equipment and current transformers located in the test cell, and the sand bags and tarpaulin outside and at the top of the test cell, was undamaged. Arc durations were unexpectedly long as compared with those found in factory tests. This caused gas to escape around the blade, and produced the faults to ground. When the breaker was returned to the test laboratory, it was found possible to reproduce this condition, and it was then discovered that alterations inadvertently made just prior to the field tests had interfered with the full flow of air across the contacts and had, therefore, resulted in long arcing time.

This specific difficulty was corrected, and, in addition, further development work, using the improved synthetic method of testing as the basis of study, produced other refinements in the design of the interrupting structure, vastly increasing its margin of safety. A breaker with these refinements performed perfectly in a second series of field tests in May 1941.

Description of Breaker

The breaker which successfully passed this second series is shown in Figure 1. As in the earlier breaker, the mechanism and air tank are mounted on top, the contacts and arc chute of the individual phases are mounted in separate steel compartments immediately below, with provision for conveying the exhaust gases up past the mechanism to openings at the top.

Without any sacrifice in operating performance or efficiency, this arrangement of major components provides adaptability to back or bottom connection equal to that of any oil circuit breaker.

Figure 2 shows one of the arc chutes with one side plate removed. The stationary finger contacts are shown in the bottom of the left-hand chamber. The moving blade passes down through openings in the top walls of this chamber to make contact with the fingers and thus close the circuit. To interrupt the circuit, the blade contact is withdrawn upward, and a blast of air is blown across the arc from the opening visible at the left. This forces the arc against the cross barriers on the right-hand side of the chamber where it is extinguished at current zero.

The arcing chamber through which the blade passes is narrow, and beyond the leading tips of the cross barriers, the path expands rapidly, both horizontally and vertically so as to increase the cross-sectional area available for exhaust of the gases. Beyond the stacks of copper cooling plates all four passages lead into a common chamber whence the gases are carried off by the vertical exhaust tube.

In the full open position the moving blade pulls clear out of the opening in the top of the chute. In order to prevent the escape of gas around the blade at some intermediate position during the inter-

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system, this is probably not an important factor. The effective ground plane in regions of high soil resistivity can be raised to the earth's surface by laying counterpoise wires to distances from the shielding masts of two or three times their height. However, in most cases it is probably more economical to increase the height of the masts.

Local terrain conditions are usually more important. The previous investigation[1] showed that, for transmission lines the nominal protective angle, which is defined as the angle between the vertical and a line joining the conductor and ground wire, should be modified for lines constructed on hillsides. The true protective angle for this case should be the angle between the perpendicular to the side slope and the line joining the conductor and ground wire. Similarly for the application of the data presented here to substations on hillsides the dimensions h, the shielding mast height, and d, the height of the protected object, should be measured perpendicular to the earth's surface. The distance x between the object and shielding mast should be measured along the earth's surface. This is illustrated in Figure 12.

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rupting operation, an air lock has been provided in the form of a chamber about the blade opening. This chamber is fed by fresh air bled from the blast stream back of the orifice. There is also a corrugated turbulent seal both between the chamber and the chute and between the chamber and the outside air.

The operating mechanism is of the pneumatic type utilizing air for both the closing and the opening operations. It controls the blast valve in such a way as to insure that the air blast is available prior to contact separation.

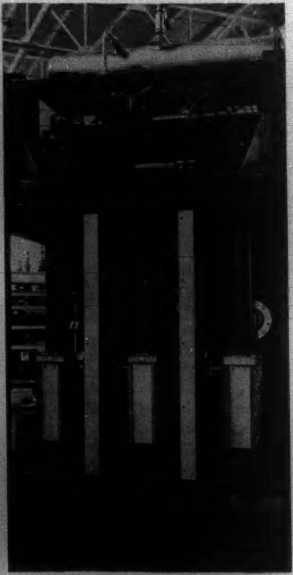


Figure 1. The air-blast breaker

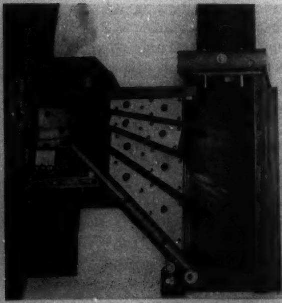


Figure 2. The arc chute with one side plate removed

Factory Tests—the Synthetic Circuit

There has previously been described before the Institute[2] the so-called synthetic method of making short-circuit tests on interrupting devices. This method consists essentially of passing heavy current at low voltage through interrupting contacts and causing high or normal recovery voltage to be applied across the contacts at the instant of interruption at current zero. The new and improved circuit, which differs somewhat from that previously described, is shown in Figure 3. In this case the high-voltage source is connected through to the circuit breaker under test and is subject to practically full excitation at all times. This facilitates proper timing in that it causes the recovery transient from the high-voltage circuit to appear automatically upon momentary extinction of the arc in both the test breaker and the auxiliary breaker rather than making the appearance of this transient dependent upon the breakdown of a gap.

Referring to Figure 3, the heavy lines indicate the high current circuit. This passes from two terminals of the generator through

1. A station breaker for finally de-energizing the circuit.
2. Reactors for controlling the current magnitude.
3. The test breaker and an auxiliary breaker in series.

One terminal of the test breaker is grounded. The auxiliary breaker must be arranged to interrupt at the same time as the test breaker. A second pole of the same breaker or another identical breaker is usually the most convenient thing to use.

The high-potential circuit starts from the third terminal of the generator, passes through a reactor, a step-up transformer and a resistor to a capacitor. By manipulation of the resistance and reactance of this circuit, the magnitude and phase angle of the voltage at the capacitor is subject to close control. The usual phase-angle adjustment is such that the capacitor voltage leads the current through the test breaker by 90 degrees so as to give the same phase relationship between voltage and current as the conventional circuit. The high-voltage terminal of the capacitor is connected through a resistance and in-

Figure 3 (right). The synthetic testing circuit

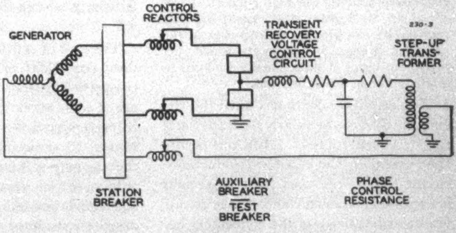


Table I. Summary of Test Results

Test No.	Phase	Type of Test	Currents First Cycle	Currents Initial in Arc	Kva	Arc Length Cycles	Arc Length Inches Per Break	Trip to Interruption Cycles	Short-Circuit Duration
1A	A B C	O	41,000 42,000 26,000	24,000 24,000 23,000	600,000	0.2 * 0.2	0.4 * 0.4	4¼	6.2
A	A B C	CO	38,000 28,000 32,000	23,000 22,000 23,000	580,000	0.2 * 0.2	0.4 * 0.4	4¼	6.2
3B	A B C	O	89,000 64,000 69,000	48,000 47,000 47,000	1,200,000	0.4 0.4 0.2	0.9 0.9 0.5	4½	6.1
4B	A B C	CO	67,000 60,000 80,000	46,000 47,000 47,000	1,180,000	0.4 0.2 0.4	0.8 0.3 0.8	4½	6.2
5C	A B C	O	72,000 76,000 94,000	60,000 61,000 62,000	1,560,000	0.5 0.5 0.2	1.0 1.0 0.5	4½†	3.3†
6C	A B C	CO	85,000 111,000 74,000	58,000 59,000 58,000	1,480,000	0.1 0.3 0.3	0.2 0.8 0.8	4	5.5
7D	A	O	57,000	26,000	200,000	*	*	4	14.5

*Cleared with contacts separated, but still overlapping.

†Breaker pretripped.

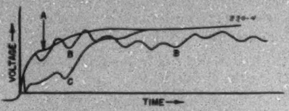


Figure 4. Recovery-voltage transients

A—Synthetic circuit, breaker clearing
B—Conventional circuit
C—Synthetic circuit, breaker failing

ductance to the ungrounded terminal of the test breaker. The magnitude of the resistance in this circuit was usually such as to give a current between 5 and 50 amperes. The inductance was much smaller in ohmic value than the resistance and so had no appreciable effect on the current in this connection. Its purpose was to introduce a controlled amount of oscillation into the recovery transient at the test breaker.

For a circuit-opening test, this circuit operates as follows:

Both the test breaker and the auxiliary breaker are initially closed. The station breaker closes applying the short circuit. This allows current to flow in the high-current circuit and also energizes the high-voltage circuit, setting up voltage at the capacitor and current in the resistance-inductance circuit between the capacitor and the test-breaker terminal. This current leads that in the high-current circuit by about 90 degrees, and causes the current in the test breaker to lead that in the auxiliary breaker by an angle which, expressed in terms of time, usually amounts to a few tenths of a microsecond.

This condition continues until the circuit breaker contacts are separated and the arc is interrupted. When this occurs the current in the resistance-inductance circuit has its previous path to ground interrupted and must complete the circuit via the capacitance of the connection between the two breakers. Were there no inductance in this circuit the voltage across the test breaker would respond by rising along a logarithmic curve to the voltage of the capacitor, without oscillation or overshoot. By inserting a small amount of inductance, however, a controlled amount of oscillation can be obtained, so that it is possible to obtain a range of recovery characteristics quite representative of a good number of those occurring in actual service. Curve *A* of Figure 4 shows a cathode-ray oscillogram of a voltage-recovery transient actually obtained in this manner for simulation of the duty represented by curve *B*, which was obtained on a conventional test circuit.

In common with other synthetic circuits, the present one gives the breaker an opportunity to extinguish the arc at a given current zero or allow it to reignite. Whether extinction or reignition takes place is indicated on the cathode-ray oscillogram. Extinction is indicated by the rapid establishment of voltage across the breaker contacts and the absence of breakdowns on the voltage-recovery curve. Failure to extinguish is indicated by a delay in voltage establishment or breakdowns on the voltage-recovery curve. Curve *C* of Figure 4 shows a cathode-ray oscillogram indicating failure to extinguish for comparison with curve *A* showing prompt extinction.

Severity of the Synthetic Circuit

The goal of synthetic testing is to produce conditions of arc current and recovery voltage for a circuit breaker which are of equal severity with those that may be encountered in service in an interruption of short-circuit current. Shapes of the recovery-voltage characteristic occurring in service vary widely, but reasonable simulation of a service characteristic may be considered to be achieved in the synthetic test if the voltage rises to the same value in the same number of microseconds without necessarily duplicating all oscillations.

The circuit has been criticized occasionally on the basis that the use of two interrupting units in series reduces the duty on the test breaker. In modern a-c circuit breakers the current is allowed to flow substantially without hindrance until it reaches the normal cyclic current zero. The insertion of a second interrupting unit will increase the effective arc voltage, but this will cause only a slight modification of the current wave, and when the tests are properly evaluated, this modification tends, if anything, to result in a synthetic test which is too severe rather than too lenient. This is explained by the following analysis of the ways in which the modification may manifest itself.

The additional arc-voltage effect may

1. Reduce the crest value of current reached during arcing. Any error from this source can be nullified by evaluating test results in terms of the current actually reached rather than in terms of what would have been reached had only one breaker been present in the circuit.
2. Shorten the current loop. Misleading conclusions from this effect can be avoided by interpreting the test results in terms of the ability of the breaker to interrupt on a current zero occurring at a given contact separation.
3. Cause the current to fall to zero more rapidly at the end of the loop. This will result in a higher average current during the last thousand microseconds or so before current zero. Consequently, it will tend to increase the difficulty of clearing when the current zero occurs.

The duty of the test breaker might be considerably reduced if the auxiliary breaker were in series with it for the recovery voltage. As will be apparent from inspection of the circuit, however, this voltage is applied across the test breaker alone.

Tests made for comparison of the severity of the synthetic testing scheme with that of conventional tests confirm this analysis. The air-blast breaker is well adapted to such tests, for, in the reduction of tank pressure, it offers a convenient means of adjusting the interrupting ability of the breaker to the power available from a conventional circuit. Further tests may then be made with the synthetic circuit set up to simulate this conventional circuit.

Tests of this type have been made on two different breakers. The first of these breakers was rated 500,000 kva at 15,000 volts, so that tests could conveniently be made close to full rating, and the second was very similar to the breaker finally tested in the field and described above.

The first group of tests was made at from about 10,000 to 13,000 amperes with 14,500 volts across a single pole of the breaker. The results are shown in Figure 5. Here the contact separation corre-

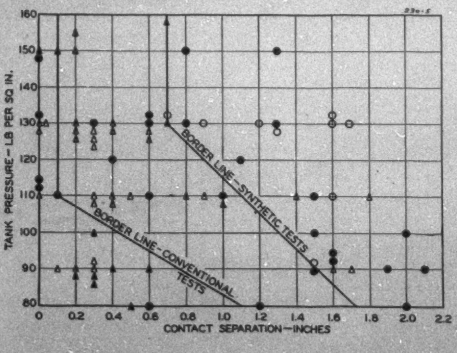
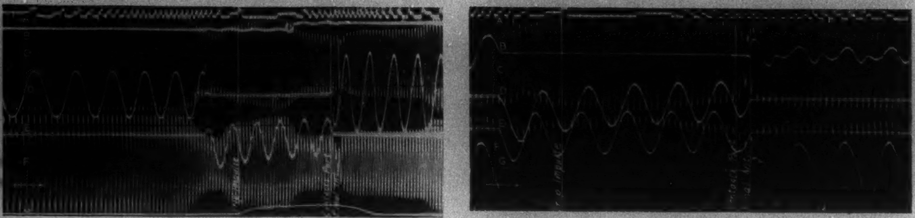


Figure 5. Conventional and synthetic tests on an air-blast circuit breaker

10,000–13,000 amperes 14,500 volts
Conventional circuit
● Arc extinctions
▲ Reignitions
Synthetic circuit
○ Arc extinctions
△ Reignitions



Curve A—Breaker travel, each step = approximately 0.17 inch
Curve B—Cathode-ray oscillograph trip
Curve C—Pressure in manifold, pole 2
Curve D—Voltage across contacts, pole 2
Curve E—Current, pole 2
Curve F—Pressure in manifold, pole 3
Curve G—Trip-coil current

Figure 6. Oscillograms of test 6-C

Curve A—Breaker travel, each step = approximately 0.17 inch
Curve B—Voltage across contacts, pole 3
Curve C—Pressure in throat, pole 3
Curve D—Current, pole 3
Curve E—Current, pole 1
Curve F—Pressure in throat, pole 1
Curve G—Voltage across contacts, pole 1

sponding to a given current zero is plotted as abscissa and the tank pressure used in the test is plotted as ordinate. Points are coded according to whether the arc was extinguished or reignited at the current zero in question and according to whether the test was conventional or synthetic. A few of the points indicate clearing at zero contact separation. This occurs because, with the blade and finger contacts, contact separation is measured from the point where the tip of the blade is flush with the tips of the fingers rather than the point of metallic contact.

On this plot one curve is drawn to indicate the border line between extinction and reignition as shown by the synthetic tests, and another to indicate the corresponding border line for conventional tests. There are not enough test points to determine both curves with high precision throughout their length, so there may be some question as to whether the curves should be exactly as shown. With any reasonable change, however, the border line for the synthetic tests lies to the right of the border line for the conventional tests, showing that at any given pressure more contact separation is required to clear the synthetic circuit than the conventional circuit. These tests, therefore, indicate the synthetic circuit to be somewhat more severe than the conventional.

The second series was made at about the same current and voltage as the first series, but at a higher recovery-voltage rate and on a breaker of higher interrupting rating similar to the breaker tested in the field. In this case the line of demarcation between extinctions and reignitions lay between 70 and 90 pounds per square inch both for the conventional circuit and for the synthetic circuit, indicating approximate equivalence of the two circuits.

These series may be taken as concrete evidence that the synthetic circuit produces test results sufficiently close to those obtained conventionally to serve as an acceptable procedure for determining ability of a circuit breaker to interrupt at the first current zero of arcing.

Synthetic Tests at Breaker Rating

After making conventional tests on this breaker up to the limit of the laboratory both at 14,500 volts and at a reduced voltage with currents extending up to and beyond the rating, the synthetic circuit was applied in order to obtain performance data upon the application of both rated voltage and rated current in the same test.

Tests were made with a restored voltage of about 15 kv rms across a single pole of the breaker and at currents varying for the most part over the range from 50,000 to 62,000 amperes. Under these conditions with a tank pressure of 150 pounds it was found that occasionally the breaker would not clear at the first current zero after contact separation. Increasing this pressure to 200 pounds sharply reduced the probability of carrying over for extra loops of current, and at 250 pounds in all cases the breaker was found to clear at the first zero point after the contacts had separated 0.4 inch.

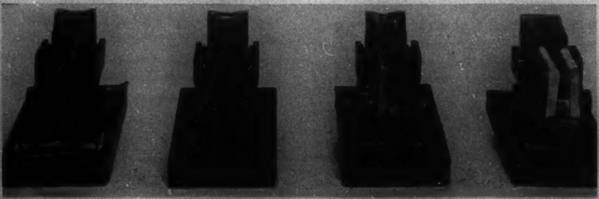
Other tests made by varying the rate of rise of recovery voltage, keeping all other conditions the same, indicate that the tank pressure required to insure clearing at the first current zero increases as the circuit recovery rate goes up.

The use of 250 pounds of pressure instead of 150 pounds, therefore, provides an increased margin of safety in insuring half-cycle arcing time for high-capacity breakers under conditions of high rate of rise of recovery voltage, and since it imposes no economic or operating burden on this type of breaker, this pressure was selected for the second series of field tests.

Second Series of Field Tests

The results of the field tests on the modified breaker are shown in Table I. The series consisted of an opening and a closing-opening test, three-phase, at each of three steps, at approximately 40, 80, and 100 per cent of full interrupting rating. Root-mean-square currents in the first cycle on the closing-opening tests varied from 28,000 amperes to 111,000

Figure 7. Stationary contacts after test, with new contact



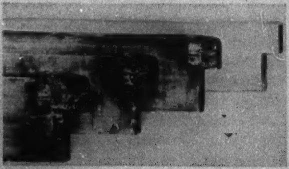


Figure 8. Movable contacts after test, with new contact

amperes. These three-phase tests were followed by one single-phase operation at about 350,000 kva which was a system, and not a breaker test. All of these tests were completely successful. The breaker operating time ranged from 4 to $4^1/_2$ cycles. The arcing time never exceeded one-half cycle, confirming the prediction of the synthetic tests made in the factory. There was no sign of fire throw either from around the contacts or from the vents on either opening or closing-opening tests.

Figure 6 shows the oscillograms of test 6-C.

The condition of the contacts from all three phases (compared with new contacts) is shown in Figures 7 and 8. Burning of the metal is detectable for a short distance up the trailing edge of the blade, at the hump of the arcing fingers, and on the arcing plate at the back of the finger assembly. Loss of material was very light, however, and conservative estimates placed the useful life of the contacts before requiring replacement at several times this amount of service.

Figure 9 shows the interior of one of the arc chutes following the tests. Here also burning is very light. The piece most severely burned is the first fibre barrier, and this could be expected to last approximately as long as the contacts.

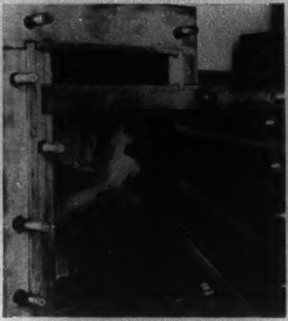


Figure 9. Interrupting chamber after test

Conclusions

1. Although assuredly unpremeditated and unscheduled, and at the moment extremely disappointing, the eventual result of the difficulties on the first series was to build up confidence in the air-blast breaker principle, for there resulted no damage whatever to adjacent equipment, and very minor damage to the test breaker itself.

2. A 15-kv, 1,500,000-kva air-blast breaker has demonstrated its ability to perform satisfactorily over the entire range up to full rating, and to close currents as high as 111,000 amperes, under field conditions, in the first such tests ever made at generator voltage.

3. Close agreement has been demonstrated between the results of factory synthetic tests for high-capacity circuit breakers and field-interrupting performance. Recognizing that such field tests, however desirable, can serve as only an occasional check on laboratory results, this agreement is significant in that it establishes confidence in the present method of developing high-capacity interrupting devices.

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1942 Supplement to Electrical Engineering—Transactions Section

Electric-Power Distribution Systems in Wartime

PHILIP SPORN
FELLOW AIEE

I—Introduction

WE are living today in some of the most critical times in the world's history. Throughout a large portion of the world men and machines are engaged in titanic struggles that may, and undoubtedly will, affect the future of Western civilization as we know it, for a century, perhaps for centuries. We, ourselves, in this country have, against our will, been drawn into this world reaching cataclysm. We are now dedicating a large part of our energies and resources and even our lives to the task of assuring that the democratic idea and way of life will emerge victorious over its counterpart and deadly enemy—the totalitarian idea. We are at war.

I have referred to the dedication of a large part of our energies. But more specifically I meant to emphasize—energy. For truly, as in no other armed clash in the history of civilized man, this is a struggle between opposing horsepower. Consider, for example, the fact that our monthly output (August 1941) of airplane engines alone has reached the figure of four and one-half million horsepower. Add to it, the engine horsepower of tanks, trucks and the numerous auxiliary vehicles going into direct military effort, and the power being built monthly for the propulsion of the numerous varieties of ships being built by, and for, the American navy, and the supreme importance of the horsepower in this particular defense program becomes strikingly clear.

Equally significant are some figures from the electric power industry—public and private. During the 12 months ending September 1941 it generated some 159 billion kilowatt-hours. As of the same date it had an installed capacity of over 42,950,000 kw. Further, it is expected that during the year 1942 there will be added over 3,000,000 kw of capacity and during 1943 close to 2,000,000 kw. One single system alone—the electric system with which the author is associated—has added during the past year over 230,000 kw of steam electric capacity, and it has under construction and will add during 1942 and 1943 over 500,000 kw of additional capacity.

All of the vast reservoir of power is designed to back up the man power of the nation and to make certain that energy, to the fullest extent that energy can be used to complement the human effort, is available to meet the requirements for increased production of all the materials needed to wage modern war.

Most of this energy has to be distributed before it can reach the utilization point. It has, in other words, to utilize the facilities of a distribution system. It is a well-known fact that the distribution system represents the largest single item of plant investment in any well-balanced electric utility system. Hence, it represents the largest aggregation of skilled effort and materials. But materials composing the distribution systems, and particularly items like copper, steel, and electrical assemblies of these, represent some of the most critical materials in the present all-out defense effort. The factories normally engaged in manufacturing these materials into finished usable products to take their place in a distribution system are, in a great majority of the cases, engaged also in manufacturing similar materials, or materials of a sufficiently closely similar nature, so as to require the same human and machine facilities, for direct use in vital defense instruments both on land and in war vessels of various kinds.

Distribution systems, as we know them, and as they exist today, are the products of concepts and ideas developed into plans, and built of materials and labor utilizing the concepts and ideas of human beings, generally engineers. These concepts and ideas, in turn, have usually been based upon knowledge of physical and electrical requirements of a distribution system and of the corresponding abilities of certain materials to meet those requirements. In many cases, however, the requirements themselves, although the result of a great deal of experience and a knowledge of fundamental engineering characteristics, are based upon more or less arbitrary criteria of what is required to give satisfactory performance. For example: It is apparent that, while there is much experience and sound distribution engineering behind the common criterion of designing distribution feeders for two per cent voltage drop to the distribution center or point of primary takeoff, it is possible to design, build, and operate distribution systems with higher voltage drops. This is but one example illustrative of the fact that many parts of distribution systems have been built to meet more or less arbitrary standards.

This is not said in derogation of distribution engineering as currently practiced. We all recognize that certain standards of performance are prerequisite to any rational design of any distribution system, but many of the standards are not only based upon judgment as to the level of performance that the standards have to reach, but, in many cases today, are the

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product of a continuous raising of such levels. Only to the extent that they do not represent the minimum that can be safely used, particularly in times of necessity, do I mean to label them as arbitrary standards. To that same extent, the limitations from a load-carrying standpoint are, in many cases, man-made and they, therefore, offer possibilities of being unmade and their use expanded in times of necessity, by man.

Considering the necessity of utilizing all available effort to the utmost in a war for survival, it is obvious that, to the extent that distribution systems, so vital to the delivery of power and energy to all war industries, have to be expanded in order to meet the increasing need for electric power and energy, it behooves us at this time to re-examine the peacetime ideas and concepts behind the planning and building of our distribution systems. It is necessary to ascertain how far ideas that were perfectly sound only a short time ago are still sound today and how far they can be modified; to what extent new ideas can be used, particularly for the duration of the emergency, so as to reduce to the practical minimum the total efforts expended on expansion of distribution systems.

II—The Object and Scope of This Symposium

The object and scope of the symposium, to which this paper forms an introduction, is to survey, study, and analyze all phases of the distribution system with a view to not only developing new ideas and concepts, but even more so to altering previous concepts of what has to be done on a distribution system under conditions facing the electric supply industry and industry of the country in general, today: to determine means and methods that can be employed for adapting, expanding, and operating existing distribution systems for the maximum broad overall benefit. This will recognize first, that all distribution systems (at least distribution systems that have maintained what might be called normal standards of adequacy) have inherently considerable elasticity, or rubber, or stretch, to use more common terms. A good deal of that stretch, it would appear, can and must be used under present conditions. The exact methods of utilizing such elasticity, the ways and means, will be developed in detail in the principal papers. Again, it will recognize that, in many of its aspects, distribution engineering as practiced today is a result of slowly developed standards and concepts of what is adequate quality of service. Side by side with such development has been an acceptance, during the past two decades at any rate, of the idea that quality of service should be continually improved. In general, these ideas have been sound, at least for peace times. But in making such a statement, it needs to be recognized that service can be rendered on several—perhaps an infinite number of—different quality levels, ranging from the very highest level to one that is definitely below the passable or acceptable. But in between these two limits there is a broad band for latitude. Where a national emergency exists and where the national interest dictates the desirability of modifying such standards of quality, there most certainly is every reason for analyzing *de nova*, determining, and then adopting a newer concept of what is adequate for the emergency period.

Not only in operation, but in maintenance also, a closer examination of the true needs of a distribution system is in order. More than ever in times of a national emergency, the question of indispensability of any proposed program needs to be gone into thoroughly. Not only that, but the long-term concept, even the reasonably long-term concept, may have to be disregarded and be permitted to give way to the concept of immediate or reasonably immediate need. It is true that all this may result in somewhat greater eventual cost. Deferred maintenance, for example, always results in greater ultimate costs. But here again, there is considerable margin of latitude between what might be called clearly deferred maintenance and what is merely anticipatory maintenance. The latter will frequently result in a total saving over a long enough period of time. However, under the situation confronting the country today, a program involving somewhat higher ultimate costs may be the more sound, and therefore, the desirable one, if not the necessary one, to follow.

From the standpoint of the materials situation and the help that can be given here, it is necessary to give consideration to the fact that many defense industries are in direct competition for the same or allied materials used on a distribution system. This is true, for example, in materials like copper, steel, tin, zinc, and any number of others. In other cases, the competition extends beyond the raw material and enters into the manufactured product itself. Typical examples of that are transformers, electric cables, circuit breakers, protective devices, and numerous others that can be cited. Hence, the need to critically examine not only the extent to which existing materials and devices can be more intensely employed, but where new materials or devices are needed, to determine what substitutes for critical materials might be employed. This symposium will concern itself with this also.

The development of the idea of the importance of the distribution system to the task of making available adequate electric energy to successfully prosecute the war leads directly to the thought that the defense or the protection of that system is likewise a most vital matter. But this subject is a difficult one to handle, particularly in a public forum. A discussion of weaknesses of any system is an automatic indication of the logical point of attack by those sufficiently motivated to carry on the work of the saboteur. Hence, such a discussion has to be, of necessity, most limited. But such limitation of the scope of the discussion does not alter the fact that certain plans, ideas and ways and means of providing secondary lines of defense, so to speak, can be developed, and in many cases have been developed and put into effect. To that extent, discussion is not only possible but can be fruitful in bringing about greater safety.

III—The Limitations of the Symposium

Even the brief discussion of the distribution problem that has been given here, should have indicated fairly conclusively that the problem undertaken is a complex one. What is of even greater significance is the fact that the distribution system, more perhaps than any other part of an electric supply system, is a local problem. It is a polygon full of local angles. All of this has been fully recognized. The fact still remains, however, that certain principles are more or less universally applicable. These, in particular, it will be the aim and purpose to explore.

In arranging the series of papers here presented, and in their writing, it has been neither the aim nor the intention to prepare a complete text on the subject, or on any phase of it. On the contrary, it is fully recognized that a task of that sort would be practically impossible, and certainly so, within the time and the space that has been made available for that purpose. Rather has it been the thought that this series of papers will result in a stimulation of thinking along new avenues and that that will lead to further work that will be undertaken. This work will have to be done in the scores, possibly

Underground Distribution Systems in Wartime

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Synopsis: This paper reviews the underground distribution system, suggests methods to conserve existing facilities and ways of utilizing latent capacity, and summarizes some of the known information available. Additional information is given on duct-bank load limitations and on time-temperature factors for cable and duct.

THE war program requires that we take every advantage of existing equipment and unused capacity to carry the unusual loads which are being supplied. Unpredictable new loads and load increases must be supplied promptly, although materials and equipment progressively become scarcer. Expedients will be necessary which would not be considered during normal times and all of our combined ingenuity will be required.

The experience from previous operation must be utilized to the greatest extent, but since the conditions at present are becoming so extreme, previous experience under normal conditions may be of value largely as a sign to point the direction. The opportunity to obtain valuable data must not be overlooked. Field studies, investigations, and surveys should be continued or expanded so that the road can be mapped as we travel over it. Information gathered now will be of great value for present operation and future design.

Method of Supply

In those cases where a radial distribution system is used advantage may be gained, both in capacity and in regulation, by changing to a network system. A properly designed network will increase the capacity of existing facilities in a given area and improve the service reliability. If maximum use is made of the capacity increase, there will be very little voltage improvement. However, it may be necessary, for the duration of the emergency, to modify our existing concepts of satisfactory voltage regulation in order to make available the maximum capacity to carry the load. This does not necessarily mean exceeding the existing limits of minimum and maximum voltage, but it may mean a wider band than has been considered good practice. The change from radial to network will only be economical in certain cases. In general, the increase in capacity alone will not justify the expense of changing over, and the material required may be more than to obtain the same capacity increase in other ways.

A radial system usually has a number of dual-service customers supplied from it. Standard practice requires that capacity must be available on each service for the entire load of the customer. This reserves *normal* capacity of twice the load being supplied. More liberal use of the emergency capacity of the system will permit this load to be supplied normally from one feeder without overload, but will not reserve capacity on a second cable so that when the second supply is required the emergency rating of the cable will be used. In some cases, additional load transfers at the time of the emergency can be made to relieve the overload. Emergency service may have to be restricted to the customer's essential load. As capacity limitations become more critical, it may become necessary to make available emergency service only to essential war industries, although this does not appear necessary at this time. Control of the use of emergency service by the utility instead of by the customer will provide greater flexibility for the use of existing capacity. A review should be made of the facilities supplying all dual-service customers to release all possible capacity for new loads and increased loads.

Additional capacity may be made available by the parallel operation of primary feeders. This must be done with care so that a large area will not be without service in case of a cable failure. The interrupting duty on equipment connected to such parallel lines will be materially increased, and fuses and circuit breakers may be inadequate. The same advantage may be gained by transferring customers between feeders, in order to obtain the highest load factor. As an example, if there are two feeders into an area, one industrial and the other commercial and residential, it may be advantageous to change both to combination feeders.

Reliability of Supply

All reasonable precautions should be taken to guard the system against damage to cables and accessories. This damage may be intentional or unintentional and may be mechanical, thermal, or flood. Mechanical includes both external and internal explosions as well as other types of mechanical damage. Thermal includes fire as well as overload.

Damage should be prevented if possible, but when it does occur, the organization must be prepared to repair it promptly and restore the equipment to service. This will require suitable trucks and trailers equipped with splicing materials and pumps. Careful study should be given to the amount and efficiency of this equipment.

Fire fighting equipment mounted on a truck or trailer should be provided which can be quickly moved to any part of the system. More than one may be required for complete coverage of a large property. Mobile fire fighting equipment of the CO_2 type has been designed and is available.[1] This is useful not only for manhole and vault fires, but also for other types of fires on the system. An ample reserve supply of CO_2 should be kept on hand at all times.

Fireproofing of cables in manholes and vaults should be very liberally applied to minimize the danger of a fault being communicated to adjacent cables and to

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hundreds, or even thousands, of distribution systems.

The experience in the application of these ideas, and the development of new ideas that effort along this line is bound to bring, will eventually give a great deal more knowledge and information. But there is no time to await the full gathering of that experience. Even though all the facts necessary for a complete solution of all the phases of the problem are not available at the present time, a beginning must be made at once. All engineers, and distribution engineers among them, must pitch in and contribute their share to the common task.

reduce the damage which will result from a manhole or vault fire. In special cases, it may be advisable to install barriers to segregate one group of cables from another. A highly important transmission cable in the same manhole with old distribution cables might justify special precautions.

Spare materials must be checked and watched to assure ourselves that any reasonable emergency can be handled promptly and efficiently. A proper stock of cable in the various lengths required should be given careful study. The stock of reserve cable should be higher than normal, due to the probability of a higher failure rate with heavier loads, the possibility of extensive damage under emergency conditions, and the extended delivery required on new cable. Other spare materials should include complete kits of splicing material, and necessary switches, transformers, fuses, network protectors, potheads, and any special equipment such as oil reservoirs, gas pressure tanks, and various types of fittings. Emergency conditions caused by explosions, washouts or cave-ins, and fires must not be overlooked. Manhole frames and castings should be available. Consideration should be given to using precast manholes for emergency replacement in cases of extended damage. The installation of precast manholes has already been shown to be practical and economical[2] and may meet certain emergency conditions.

Fault-locating equipment and methods should be checked. Fault-locating equipment is available for distribution cable which will generally give a prompt and accurate location. This is necessary before much other work can be done to start repairs.

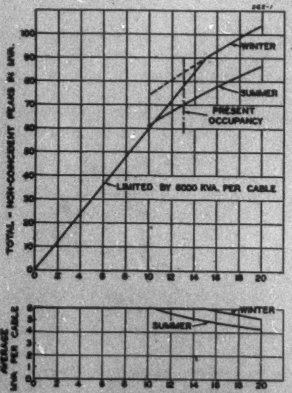

Figure 1. Duct-bank thermal capacity

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable

Repairs must be made as quickly as possible. Temporary repairs which have to be remade later are usually not justified. This has been verified by incomplete reports from England. Some experiments have been made with a "cold-setting" material for making a temporary high-voltage cable joint which does not require a lead wipe.[3] This has been found to be practical and usable where conditions require it, such as with very extensive damage requiring prompt repair with minimum skilled labor.

Salvage methods should be reviewed so that maximum advantage is obtained from removed cable and material. Duct splices have been found uneconomical for short lengths of cable but may be necessary to conserve material and maintain emergency stocks. Cable which is unfit for use at its rated voltage may be usable at lower voltage. Material must not be discarded if it can be reused to conserve new material.

A supervisory and alarm system giving temperature and pressure indication and alarm is a tool which can be used to obtain the maximum capacity with safety from a given system. Temperatures and pressures can be watched and the necessary steps taken to relieve any unsafe conditions as they arise. All such systems which have been installed or are being installed should be checked to see that the maximum benefit is being obtained from them. By combining supervisory indication with remote control of switches, a very flexible system will result.

Underground Equipment Limitations

A considerable amount of research has been done on the underground system. Cables, ducts, manholes, transformers, and cable accessories have been studied and related to each other and to the ratings which may be used with safety. Additional work is in progress in many of the companies. In presenting the following material, liberal use has been made of the information now available, some of which has been previously presented to the Institute.

Table I

	Ratings in Amperes (2-Phase 3-Wire—A and C Conductors)		
		Emergency	
	Normal	2-Hour	4-Hour
As received from field	160	240	200
After reconditioning and silver-plating	200	300	250

The above ratings are based on maximum temperatures as follows:

	Temperature—Degrees C.	
	Normal	Emergency
Contacts under oil	70	90
Terminals above oil	85	90

Circuit capacity may be limited by apparatus in the circuit, such as switches. Tests were made at Philadelphia on a subway-type oil switch rated at 200 amperes. These tests revealed that the current-carrying capacity of the switches was relatively low, although their appearance was good. Silver plating of the contact surface was found necessary to put them in satisfactory condition. Table I summarizes these tests.

The *B* phase leg was the limitation in each case. The *B* phase current is approximately 1.4 times the *A* or *C* phase, so that the switch would be rated somewhat higher on a three-phase system.

Equipment of this type, which imposes a capacity limitation on the cable, should be rebuilt, replaced, or removed. These switches are being replaced with a "drop-out" switch in all cases where there is a circuit limitation. The new switch has adequate carrying capacity and may be repaired or replaced without interruption to either cable connected to it.

Transformers and transformer vaults may be a limitation either on a network or on a radial system. In general, underground transformers have been operated at or less than name-plate rating whereas overhead transformers are operated considerably in excess of name plate. The peak load on network transformers averages only 50 per cent of name-plate rating,

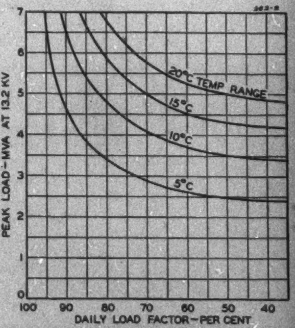

Figure 2. Allowable daily peak load for various conductor temperature ranges and daily load factors

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable although individual units may operate close to capacity. This is due to fault capacity requirements, provision for load growth and use of standard sizes. However, individual transformers in a network may become overloaded and require relief. Vaults designed for a single bank may be large enough to install a second bank or a three-phase transformer. Either operating above name plate or operating with excess capacity in the vault will require added ventilation. Natural ventilation may be improved, or forced ventilation controlled by a thermostat may be required. Considerable reduction in ambient temperatures with a corresponding capacity increase may be obtained by ventilation improvement.

Improvement of vault ventilation and subway-transformer cooling has been investigated by the Boston Edison Company. The maximum safe operating copper temperature is established as 100 degrees centigrade at 100 per cent load, which results in a top oil temperature of approximately 80 degrees centigrade. During the summer of 1938 the problem of excess temperature on subway transformers became critical and was temporarily solved by flooding the vaults with water from the city mains. This method of cooling was entirely satisfactory as an emergency expedient, but a more permanent method of correction was necessary.

A fabricated steel grating was designed and used to replace the four-foot-two-inch-by-three-foot-two-inch solid rectangular covers in the roof of the vault. This resulted in a reduction of 14 degrees in the manhole ambient temperature, although the differential between the top oil temperature and the manhole ambient temperature remained at 22 degrees centigrade. A net capacity increase of 15 per cent resulted from the reduced manhole ambient temperature provided by the open grating.

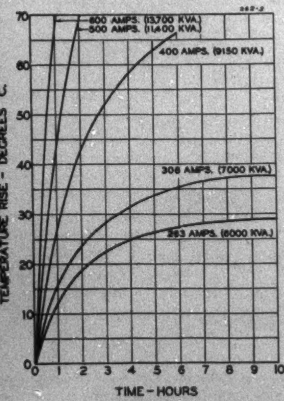

Figure 3. Conductor temperature rise above ambient

Laboratory test data

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable

The transformer loading was so high that this reduction was insufficient to reduce the hot-spot temperature to 100 degrees centigrade. Experiments were then conducted with supplementary external radiators installed on the transformers. Two cast-iron radiators of $26^1/_2$ square feet of rated radiation each were used on a 100-kva three-phase cast-iron-tank transformer. One of two transformers in a vault with the open grating was equipped with these radiators and a ten-day heat run made on each transformer. This resulted in an indicated increase in capacity of 21 per cent for the transformer equipped with radiators when installed in a vault with the ventilating grating.

These tests showed that the combination of an open grating and external radiators on the transformer is equivalent in its cooling effect to flooding the manhole with water, and results in a more permanent remedy.

Insulation Limitations

The problem of cable loading cannot be separated from duct and manhole design. In order to determine the rating of any cable, the number and arrangement of ducts, the other load in the duct bank, the length of the duct run between manholes, the depth of the duct run, the length of the manhole, and the offset in the manhole must be known. Other factors involved are the soil conditions, the earth temperature, and local heating due to steam mains or other subsurface facilities.

Valuable work on the over-all problem has been done by the Commonwealth Edison Company and the results of these investigations presented by Halperin in 1939.[4] Investigations of safe operating temperatures and of the economics of duct loading have been made by the Consolidated Edison Company of New York and reported by Franklin and Thomas in 1939.[5,6] The over-all problem should receive greater attention both in the field and in the laboratory. Present conditions will give a large amount of field experience, and adequate data should be collected. This will supply information for the operation of the system at its maximum capacity and indicate critical conditions which can be corrected before they become serious. It will also add much to our present knowledge of how far we can safely increase ratings of cable and duct. In order to do this with the greatest benefit to the industry, an agreement should be reached on definitions and basic information to be collected.

The great majority of the cable which is in service in the classification being discussed is solid-type paper-insulated cable. Only this type of cable will be considered, although some of the factors may be applied in modified form to other types of insulation.

The maximum allowable copper temperature for solid impregnated paper-insulated cable under the AIEE rule is 90 degrees centigrade minus the rated voltage in kilovolts, but not more than 85 degrees or less than 60 degrees. This rule covers normal operation of the cable. Most users have developed certain working rules for emergency conditions. In some cases, these do not cause any material temperature rise above the basic rule but depend on the heat-storage capacity of the cable and duct. In other cases, the temperature is permitted to go substantially above the recommended level for short times at infrequent intervals. "Emergency operation" is defined in the Association of Edison Illuminating Companies Specification for Impregnated Paper-Insulated Lead-Covered Cable, Solid-Type (July 1941) as operation at temperatures approaching the maximum allowable emergency temperature for one period per year on the average and not for more than four periods in any one

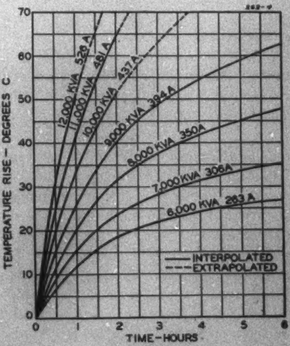

Figure 4. Conductor temperature rise above ambient

Interpolated from laboratory test data

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable twelve consecutive months, each period being not more than 24 hours. The maximum temperatures permitted are 115 degrees centigrade for cables operating up to 1 kv, 102 degrees centigrade for cables operating up to 5 kv and 96 degrees centigrade for cables operating up to 15 kv. These temperatures are being exceeded in some cases, at least one company using 110 degrees centigrade for cables up to 5 kv. Cables up to 15 kv are generally being operated at a maximum emergency temperature of 90 degrees centigrade, although some experimental work has been done at higher temperatures.[4,5]

Duct Limitations

From the above rules, ratings can be developed for cable in various types of ducts, duct formations, soil conditions, and duct occupancy. Complete empty-duct temperature surveys are the most satisfactory method of determining the ratings which should be applied to the cables in any duct bank. In applying the temperature rule literally the hot spot must be used, since no part of the cable should rise above the recommended temperature.

Duct banks have a definite thermal limitation so that many of the old 16-, 20- or larger-duct lines may be thermally loaded, even though there are vacant ducts available. Figure 1 illustrates a typical summary of the analysis of test data for a survey made in Philadelphia on a 20-duct bank containing 13 cables. The survey consists of taking daily temperatures by thermocouples installed in empty ducts and hourly load readings for all cables in the duct bank. The average readings for three consecutive days are used.

The analysis considers the existing actual load factors of the cables in the duct bank. The load factor probably will not change sufficiently in a year to materially change the results.

This chart is predicated on the fact that the upper limit of duct bank temperature (empty-duct air temperature) is determined by the rise of copper above empty-duct temperature for a selected rating of the cable. The cable involved is a three-conductor 350,000-circular-mil 13-kv belted paper and lead cable. At the normal rating of 265 amperes it has a temperature rise of copper over adjacent empty-duct air of 27 degrees centigrade, as determined by calculation and confirmed by test. With a maximum allowable normal copper temperature of 77 degrees centigrade for this class of cable, we must limit the empty-duct air temperature to 50 degrees centigrade. This agrees with the findings of Church,[10] Halperin[4] and others on the recommended maximum duct-bank temperature.

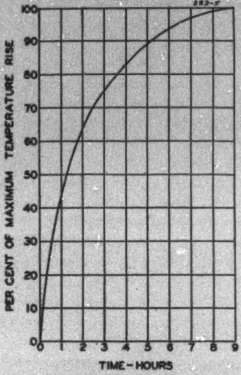

Figure 5. Heating time of cable in terra cotta duct (for all loads)

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable

Similar charts are used to determine the need for additional duct lines and have been made for many of the critical locations.

Sheath Limitations

Insulation deterioration is not a serious problem with modern distribution cables operated at temperatures well above the AIEE temperature rule.[4–8] It is estimated that the sheath life is from one half to two thirds of the insulation life if recommended temperatures are observed. Temperature variation rather than temperature level causes the major damage to the cable by causing sheath deterioration. The absolute sheath temperature does not materially change the lead characteristics at temperatures below 70 degrees centigrade. Research to extend sheath life is being carried on in the laboratory, but field experience and investigation must come from the cable users to supplement the laboratory information.

Manhole size and design have a very definite relation to cable loading. Investigations have been made and reported,[4] and additional investigations are in progress. The majority of older manholes are too small from the standpoint of cable movement, and inadequate protection is provided at duct mouths. Modern practice provides for a longer and wider manhole and for fewer ducts with improved protection for the cable at the duct mouth. Badly congested manholes in critical locations can be materially improved by a program of rebuilding to more modern standards, which should result in a material reduction in failures due to sheath breaks. Such a rebuilding program would permit higher cable ratings on the rebuilt portion and require very little critical material. Present information is incomplete, but generous manhole construction should be provided wherever the space is available. Halperin[4] has recommended a minimum manhole of 10 by 6 feet for three-conductor 500,000-circular-mil 13-kv cable, and 8 by 4½ feet for three-conductor 5-kv cable of 375,000 circular mils or less.

Small cables have less cable movement for the same temperature variation than large cables. Cable conditions can be materially improved by using three single-conductor cables of the same copper cross section in place of one three-conductor cable. This will result in a substantial reduction in the sheath damage without any decrease in the rated loading of the cable. One objection on certain applications is that the cable impedance will be increased approximately ten per cent so that such a cable cannot be operated in parallel with an existing three-conductor cable. However, on radial feeders a substantial increase of the cable rating will result from the use of single-conductor cables.

Cable movement has been related to temperature variation.[4] Cable movement has been related to sheath damage to show that increased movement results in increased damage although present information is incomplete. Factors to be considered include the size of cable, thickness of lead, type of lead, frequency of movement, size of duct, length of manhole, and man-hole offset. Halperin has shown that a long length of cable will have proportionately less movement than a short length for equal temperature variations.[4] More recent information has indicated that up to a variation of 25 degrees centigrade, the movement at the duct mouth for 800- to 900-foot lengths is the same as for 300- to 400-foot lengths. Long lengths of transmission cable have been installed in Chicago, Cincinnati, and Newark. The experience with these cables will be valuable for future installations.

Temperature variation is dependent upon load factor, since, if the load does not change, the only temperature variation is due to the duct-bank temperature change. Figure 2 shows the peak load plotted against the daily load factor for various temperature ranges. The range is the same for a 6,500-kva peak at 95 per cent load factor as for a 3,000-kva peak at 73 per cent load factor. These values are computed but field investigation indicates they are conservative. Sheath damage is a function of cable movement which in turn is a function of temperature variation. A high load factor will reduce the sheath damage and, therefore, permit higher loading of the cable.

Two customers in Philadelphia illustrate the variation that may be expected. One is an oil refinery with a load factor of better than 95 per cent. The second is an electric furnace with a load factor of less than 60 per cent. The latter is full load for approximately three hours and no load for approximately two hours, then repeat. The normal rating assigned to these cables is the same for both customers. No sheath damage is expected from the first but an extreme amount from the second. Unfortunately, the experience has been too short to be of any value.

Emergency Ratings

Ratings have been applied to 13-kv paper-insulated three-conductor belted, 350,000-circular-mil cable in Philadelphia as shown in Table II.

These calculations are based on six cables per duct bank with a 75 per cent load factor, 20 degrees centigrade earth temperature, and result in a normal-load copper temperature of 77 degrees centigrade. The emergency ratings are based on various time-temperature tests to give a maximum copper temperature of 90 degrees centigrade. Figure 3 shows laboratory test data and Figure 4 shows various ratings interpolated from the laboratory test data. The laboratory setup was arranged to simulate field conditions and was later checked in the field and found to be quite accurate. Figure 5 shows the per cent of maximum temperature plotted against time for all loads.

An interesting example of the punishment which can be taken by cable is the Delaware-Richmond 13-kv tie line in Philadelphia. This consists of six cables between two generating stations. Originally in 1926 it was given a rating of 265 amperes per cable, which, due to duct conditions, is about ten per cent above that permitted by the AIEE rule. This was operated at or above its rating for a number of years, and then in 1936 for station economy reasons the rating was increased to 289 amperes. The failure rate doubled following this increase, and in 1937 it was reduced to the original rating. The average failure rate during the period of service has been 28.5 per 100 miles, which is a high rate but, nevertheless, would result in an average cable life of 29 years. Cable-failure rates of this order are extreme and cannot be considered good practice. However, in spite of this high failure rate, the life is so long that obsolescence and system changes have to be given equal weight with service failures. In the fifteen years of operation there have been 95 failures, 28 in the first 10 years and 67 in the last five years. This latter period includes the period of increased rating. During the past $2^1/_2$ years, accurate failure records have been kept and show that 80 per cent of the failures can be attributed to cable movement. Manholes in this entire duct run are small and severely congested, and much of the damage is attributed to this. More modern manholes with adequate space would result in a very substantial reduction in the failure rate. Cable can be punished severely year after year and continue to operate. The economics of this case indicate that it has been good business to operate the cables this way.[9]

Cable rating must be distinguished from cable loading in the operation of a system. The practice in some companies is to establish a rating for the cable but permit the loading to reach only 85 per cent or 90 per cent of the rating. If the system of rating is reliable, the forecasted load should be permitted to equal the rating. In one case[6] the cable rating for 50 cables was raised to a basis of 100 degrees centigrade copper temperature, but later records showed that cable loading had been very little higher than under the old rating. However load growth will gradually change this so that additional capacity will result. Capacity will be made available by working the cable loading right up to the rating. This should be on the basis of average loading and not on short-time swing peaks.

Conclusions

Past investigations and recommendations have been based on restricted experience obtained in the laboratory or on relatively small test installations in the field. There is every reason to believe that the period we are now going through will result in creating a giant laboratory out of our present systems. Our first responsibility is to continue to carry the load and all load which is added. To do this we must search out and eliminate the "hot spots" and "bottlenecks." Material must be used where it will give the greatest return. Secondly, we should get the greatest possible return from this condition to use in future design to build better and more economical systems. An investigation of the following conditions will permit us to do both of these intelligently:

1. Earth temperatures in the vicinity of duct lines.
2. Duct-bank temperatures—studies of various types of duct banks with respect to construction and environment.
3. Cable-sheath temperatures—correlation of cable-sheath, occupied-duct, and empty-duct temperatures.
4. Cable loading—hourly load readings with special consideration to emergency and abnormal conditions. This should be related to above surveys.
5. Cable movement—the relation of cable movement, load changes, section lengths, and load factor.
6. Cable operating performance—careful analysis of cable failures and sheath breaks. Particular attention should be given to failures due to expansion and contraction and to thermal insulation failure.

Table II. Emergency Ratings—13-Kv Three-Conductor 350,000-Circular-Mil Belted Cable—Amperes

Normal—265 Amperes

	After Normal Load	After Less Than $^1/_2$ Normal Load
$^1/_2$-hour emergency	460	520
2-hour emergency	365	395
4-hour emergency	330	350

Summary

1. Provide all reasonable safeguards for the system.
2. Prepare for emergencies and rapid restoration of service.
3. Provide adequate emergency stocks of cables and accessories.
4. Maintain an adequate inspection and maintenance program.
5. Maintain complete records for use in determining present ratings and future revisions.
6. Review ratings of all cables and permit load to reach rating.
7. Rearrange load connected to cable to improve load factor.
8. Provide adequate reliability to all load by alternate routes.

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Power Supply to Distribution Substations in Wartime

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MEMBER AIEE

1. Introduction

THE purposes of this symposium in relating distribution engineering to the present war emergency have been fully introduced and set forth in the opening paper so that further elaboration will be unnecessary here. As indicated by the title, the discussion in this paper will be confined principally to transmission substations and subtransmission lines, all as a means of supplying distribution substations. An attempt will be made to explore ways and means by which more capacity can be obtained from a given amount of material and labor, both in existing facilities as well as in the design and construction of new facilities.

2. Transmission Substations

It might be said that transmission substations are the beginning of distribution, since such substations furnish the supply to subtransmission lines, which in turn supply the distribution substations. The importance of transmission substations in relation to the present war-emergency program is very great. With the exception of generating facilities, the possibilities of their becoming bottlenecks on the system may be more acute than almost any other system element, because of the larger size and more or less special design of equipment required for each particular substation. For this reason it is particularly desirable to explore the possibilities of getting more capacity from existing equipment, and to design new substations in such a way as to use material and labor most efficiently.

TRANSFORMER CAPACITY

While the problem of obtaining larger capacity from transformers is, in many respects, the same, whether these transformers are in a transmission substation or a distribution substation, there are some essential differences. For example, the transmission substation transformers are usually of much larger capacity and more or less "tailored" to fit a particular job, and, hence, there is less possibility of shuffling them around from one substation to another, as is often done to advantage with distribution-substation transformers. Also, because of the greater dependence placed upon such units in supplying not one, but a considerable number of distribution substations, relatively greater care in maintenance and in any procedure used in overloading such capacity should be exercised.

Several possibilities for getting increased capacity from transformers, some of which will also be discussed in a companion paper dealing with distribution substations, will be suggested below.

On existing self-cooled transformers it is possible in practically all cases to obtain an increase of 25 per cent to $33^{1}/_{3}$ per cent in capacity by the application of fans or blowers for forced-air cooling. Where the transformers were originally designed for such cooling, such a procedure would, undoubtedly, be adopted forthwith when increased capacity is needed, and no particular problem would be involved. Permanent blower or fan equipment should be obtainable within a few weeks.

On self-cooled transformers not originally designed for it, the application of forced-air cooling must include a check on the current-carrying capacity of leads, bushings, and so on, with a possible change, if necessary, in some of these to make sure that all parts of the circuit are adequate for the larger transformer output. In extreme emergencies, any type of fan, even a large portable fan, could be used to obtain a quick capacity increase.

In the case of self-cooled transformers already equipped with forced-air cooling, and for water-cooled transformers, an increase in capacity can be obtained by forced oil circulation with external coolers or radiators. These radiators can be air-cooled, or in extreme cases could be cooled by refrigeration.

It should be noted that to get the full cooling effect and resulting maximum capacity out of water-cooled transformers, the cooling coils must be maintained in good condition and free from internal scale or deposit. A thorough cleaning of cooling coils has often resulted in a substantial capacity increase.

The use of external water spray may be found of value in certain emergencies, particularly during hot weather. While this method does not offer a large increase in capacity, at the same time it is inexpensive where water is readily obtainable, and can be applied very quickly when needed.

Referring again to a companion paper on distribution substations, mention is made of the possibility of loading transformers on a temperature basis to values well beyond name-plate ratings, and references are made to material published on this subject. The basis of all of this, of course, is the fact that most transformers possess a certain amount of inherent overload capacity, varying with the load factor and ambient temperature conditions under which the transformers operate. Since we may be forced to use this overload capacity whether we choose to do so or not, it may be possible to do it more safely and intelligently in the light of quantitative analyses as to the effect of overloading on the over-all life of typical transformers, and from these, arrive at

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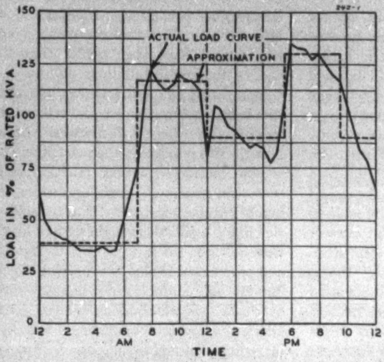

Figure 1. Typical assumed load curve for substation transformers

Load factor 67.5 per cent

Assumed peak load 130 per cent

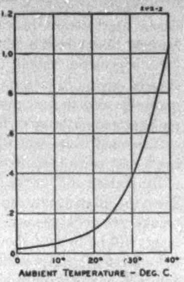

Figure 2. Daily loss-of-life curve for transformers operating on load curve of Figure 1 at various ambient temperatures

some measure of the amount of overloading which can be reasonably tolerated.

As an example of the type of analysis which can be made, Figure 1 gives a typical assumed load curve in which the peak load is 25 per cent above the transformer rating. Based upon this loading, Figure 2 has been prepared showing the loss or consumption of life in this transformer for various levels of ambient temperature. This analysis has been made on the basis of data published by Nichols and others[1,2] and obtained from exhaustive tests on the effect of temperature on the life of insulating materials in oil. It is obvious from Figure 2 that a lowering of the ambient temperature by only a few degrees very greatly reduces the effect of a given overload in consuming the useful life of the transformer.

Actually the data used in the above analysis are believed to be well on the conservative side. For one thing, the ratio between copper and core losses has been taken as 2.75, which is considerably higher than average practice. A lower ratio would obviously mean a smaller increase in heating on overload. This and other margins of safety in the figures used, along with results of field experience, tend to support the conclusion that actual transformer life would be considerably better than indicated by the analysis.

A particular instance where it may be considered entirely permissible to make rather liberal use of the inherent overload capacity of transformers would be in the case of an outage of one unit where two units are normally operated in parallel. While it should be done only on the basis of some knowledge or analysis as to the oss of life involved, nevertheless, a much higher rate of consumption of life could be allowed in this case as against the cost and, particularly during the present emergency, the availability of full spare capacity to carry the entire load. As a matter of fact, this practice, at least to a degree, would be sound economics during normal times.

Portable Car-Mounted Transformers

As pointed out above, the transformers used in transmission stations are usually fairly large and more or less "tailored" to fit a particular job, as compared with distribution station transformers. For both of these reasons, there results the possibility of a serious bottleneck to the supply of power in case of

1. A serious transformer failure.
2. A sudden demand for increased power, either at an existing location or at a new location for which it may not be possible to obtain new transformers within the available time.

To take care of such emergencies, primarily on transmission substations, there have been designed and constructed for one system, the system with which the author is associated, portable emergency transformers permanently mounted on specially designed railroad cars and arranged with a considerable number of voltage, tap, and phasing combinations to cover practically all of the transmission-substation requirements on the system. The capacity of these transformers was made as large as railroad clearances and other limitations would permit, and the entire equipment includes lightning arresters and all necessary auxiliaries mounted on the cars. With the exception that lightning arresters must be dismantled during transportation, the entire transformer is carried complete with bushings and oil in place ready to operate as soon as it reaches its destination.

Because of the extreme range of requirements to be covered by such transformers, two complementary units of approximately equal capacity and physical size were used. One of these, with a three-phase capacity of 15,000 kva, was designed for stepping down from 132 kv and 110 kv to any of the lower voltages down to and including 11 kv. Transformations from 132 kv to 110, 88, and 66 kv were obtained by autotransformer connections, while other voltages were obtained by two-winding combinations.

The other unit, having a three-phase capacity of 17,000 kva, was designed to cover the intermediate step-down ratios from 88 kv, 66 kv, and 44 kv down to lower voltages from 44 kv to and including 11 kv.

The size and weight of these transformers, approximately 85 tons each, requires each unit to be permanently mounted on a special 100-ton, drop-frame-type railroad car, which is designed, however, to meet all the requirements of the Interstate Commerce Commission and the Association of American Railroads. The cars are registered with the latter

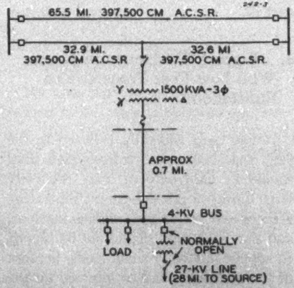

Figure 3. Small-capacity transformer tapped to 132-kv transmission line with minimum protection

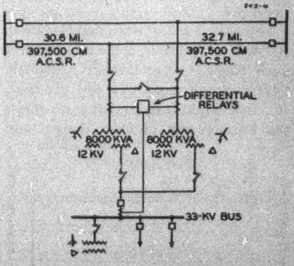

Figure 4. Medium-capacity three-phase transformers tapped to transmission line with automatic air-break switches

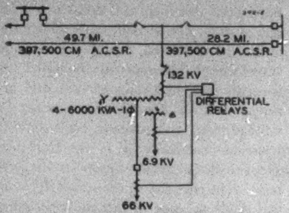

Figure 5. Large-capacity transformer bank tapped to transmission line with automatic line sectionalizing

and subject to the same obligations and privileges as any other railroad freight car.

Inasmuch as railroad sidings are not or cannot be made available at all of the transmission substations or other possible locations where it might be necessary to use these portable "universal" transformers, it will be necessary at such locations to unload a transformer from its car and transport it over highways, using special highway trailers owned by local contractors. For this purpose the weight can be materially reduced by removing the oil and some other equipment from the transformer.

Switching Equipment

As in the case of distribution stations themselves, as pointed out in a companion paper, the rapid expansion of electric systems now taking place, and which will undoubtedly continue to a large extent during the war emergency, creates a serious problem in connection with the interrupting capacity of circuit breakers at transmission stations. In many cases the growth of systems far in advance of expectations has brought about circuit-breaker duties in excess of capacity at stations fairly recently built, or where circuit breakers have recently been modernized. A number of procedures may be suggested for taking care of this circuit-breaker problem.

Probably the most economical solution, where it is applicable, is the rebuilding or modernizing of circuit breakers to increase their interrupting capacity; this, of course, can only be done in certain cases where the breakers are of older design, and where an increase in capacity can be brought about in the rebuilding process. Such a program should result in a substantial saving in material and labor and generally also in cost over the complete replacement of the circuit breakers.

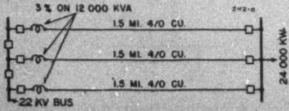

Figure 6. Original layout for 24,000-kw load

A second solution, which, in many cases, may permit the necessary expansion of the station and, at the same time, hold down the interrupting duty on the circuit breakers to avoid the necessity of replacement, will be to split the bus into two or more sections, either with or without reactors to tie the sections together. Unless the system is designed on a well-balanced basis for operating independent bus sections, it will probably be necessary to use reactors to tie the sections together. In some cases it may be necessary to employ both of these procedures, that is, to sectionalize the bus and to rebuild the circuit breakers as well.

The third, and perhaps final alternative, will be to install new and higher-capacity breakers where the existing breakers are hopelessly inadequate. This, of course, does not save either cost or material, except where it may be possible to make use of the existing breakers at other locations for which new breakers otherwise would have to be purchased. This "shuffling" of breakers, with the purchase of only larger sizes wherever possible, and the shifting of smaller breakers to lesser locations, is sound practice from an engineering and economic standpoint, not only during emergencies but during normal times as well.

Disconnecting switches and air-break switches should not ordinarily present a problem in connection with increased loading on transmission substations but should be carefully checked and maintained, along with other equipment in the station, to be sure that adequate carrying capacity is available. Overheating of such switches will lead to progressive weakness and eventual failure if not taken care of.

3. Tapping Transmission Lines With Low-Cost Transmission Substation

It has been generally believed in the past that important transmission lines should be used only to carry relatively large blocks of power between generating sources and major transmission stations, and that such lines should not be tapped to supply relatively small loads, because of the additional hazard imposed on the transmission lines. However, the economies made possible by occasionally tapping transmission lines with a relatively inexpensive step-down transformation have been so great that more and more installations have been made. Experiences with these installations, and the development of methods for installing and protecting such equipment, as well as the transmission lines themselves, have brought this practice to the point where it is now considered a very practical and successful means of providing service at many suitable locations. If a transmission line is available near the desired location, a tap on the line may be the logical and economical means of supplying loads directly, or reinforcing the supply to an overloaded subtransmission system. The alternative means of supplying a load by extending and reinforcing the subtransmission system may be much more expensive both in material and labor.

Many such installations have been made on the systems with which the author is associated, particularly for stepping down from 132,000 volts to various voltages all the way from 66,000 to as low as 4,000 volts. These transformer installations vary from the very small ones as low as 1,500 kva in capacity for serving a 4,000-volt load, to as high as 18,000 kva for reinforcing a 66,000-volt subtransmission system. The smaller installations, for obvious economic reasons, are made as simple as possible with a minimum of protective equipment. For this reason it may not be feasible to provide for clearing the transformer in case of fault in the transformer, so that it is necessary in this case to take the risk of transformer damage. Experience for a number of years, however, has been quite satisfactory in this regard. Figure 3 shows a typical installation of a small 1,500-kva three-phase transformer tapped to a 132-kv transmission line.

For larger stations and loads, more elaborate protective equipment is justified, although not the expense of high-voltage circuit breakers. Figure 4 shows a typical medium-capacity station with automatic air-break switch protection for the high side and with a differential-relay scheme for clearing the transformers in case of trouble. The two main functions of the protective equipment in this case are as follows:

1. For a transmission-line fault, the transformer is disconnected by means of its low-voltage circuit breaker from the subtransmission system, which would, otherwise, feed into the transmission-line fault through the transformer. To provide positive relaying of the transformer switch in case of line-to-ground fault on the transmission line, it is necessary that the transformer be wye-connected on the high side with neutral grounded.
2. For a fault in the transformer itself, the

transmission line is opened by its own relaying and the transformer disconnected from the low-voltage side by its own differential relays. Immediately thereafter, during an interval of approximately one minute while the transmission line remains de-energized, the transformer high-voltage air-break switch is opened up automatically from the operation of the differential relays. Following this the transmission line is restored to service.

The desirability of providing a grounded wye-connection on the high side of such a transformer may add slightly to the cost of installation:

1. By necessitating a delta-winding on the transformer if it results in a wye-wye connection.

2. By requiring the installation of a grounding transformer for the low-voltage bus if it results in a wye-delta connection, and it is also desired to establish a neutral ground at that point for the low-voltage system.

This additional cost, however, is usually considered well-justified to obtain the more complete relay protection which it affords.

While carrier-current relaying is actually employed on the transmission lines to which this station is connected, its use is not essential to the carrying out of the protective functions described above, except with regard to the ultrahigh-speed reclosing on the transmission lines. The operation of the transformer-differential relays provides a carrier impulse which stops the line from reclosing in case of a transformer fault; otherwise, the line would reclose on the transformer fault the same as for a fault on the line itself.

A still larger station with the most complete automatic protection obtainable without using high-voltage circuit breakers is shown in Figure 5. Here the transformer installation is large enough to justify the use of three single-phase units with a fourth as a spare. The manner in which transformer-differential protection, as well as transmission-line sectionalizing, is accomplished in this station, using automatic air-break switches only, is described as follows:

1. For a permanent transmission-line fault on either side of the station, the entire line is cleared, along with the low-voltage transformer breaker, and remains open for an interval of approximately one minute during which the two line-sectionalizing air-break switches are opened. Following this, both line sections reclose, the good one remaining closed, and the faulted one tripping and locking out. The sectionalizing air-break switch, on the line which remains energized, then recloses, restoring service to the transformer bank. The final step is the reclosing of the 66-kv circuit breaker on the low side of the transformer bank.

2. For a fault in the transformer bank, both the line and the transformer low-voltage breaker are cleared, but, in this case, the differential relays act to open up the transformer high-voltage air-break switch instead of the line-sectionalizing switches, after which the line recloses and the transformer bank remains de-energized. As in the station of Figure 4, carrier-current relaying and ultrahigh-speed reclosing are used on the transmission line, and, for a differential-relay operation on the transformer bank, a carrier impulse is sent out to prevent the instantaneous reclosure of the line into a transformer fault.

The satisfactory operation of these stations, and of many others of which they are typical, has fully established the soundness of this method of supplying power directly from transmission lines at relatively low cost. The use of this device wherever favorable opportunities are presented offers one possibility for supplying power to distribution substations, and, in some cases, to distribution loads directly, with the most efficient use of equipment and labor.

4. Subtransmission Lines, Existing and Proposed

The term "subtransmission lines," as used in this paper, refers to the intermediate voltage systems, such as 22 kv up to and including 66 kv, which are generally used to supply distribution substations. Such lines, of course, may be supplied either from transmission substations or direct from generating plants. The rapid growth of load brought about by the present war emergency has posed the problem of getting more capacity out of existing lines, or of providing increased capacity in one way or another with a minimum expenditure of material and labor. While a most favorable solution of the problem will, of course, depend upon the particular factors and limitations in each situation, an attempt will be made to suggest methods of obtaining increased capacity in a number of typical situations that may come up.

Short Lines—Thermal Limits Controlling

For lines that are relatively short with respect to the amount of power and voltage, the capacity limitation may be almost entirely a matter of the thermal limits of the conductors themselves, rather than limitations due to voltage regulation. In many cases, however, both of these limitations must be taken into account. Typical ways of obtaining capacity increases in situations of this kind may be suggested as follows:

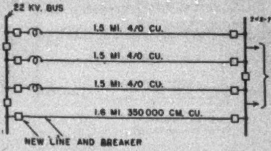

Figure 7. First step to increase capacity of lines to serve 40,000-kw load

1. Where several circuits with conductors of moderate or small size are already operated in parallel, the most economical and efficient method of obtaining an increase in capacity may be the rebuilding of such circuits with a considerably larger conductor, on the assumption, of course, that the existing conductor can be salvaged for needed use elsewhere. This method offers certain substantial savings in that

1. No additional terminal switching is required.
2. No additional right-of-way is required.
3. In the case of wood-pole lines in good condition few if any structures will have to be replaced.

If the use of larger conductors alone is insufficient to provide the necessary increase in line capacity, it may become necessary to construct additional circuits which, because of right-of-way difficulties, will usually be at least as long if not longer than the existing circuits. In such an event, the use of a larger conductor on the new circuit will be of little advantage, unless the conductors on the original circuits are enlarged at the same time. In other words, if the existing circuits are not changed, any attempts to load up the new circuits in proportion to their larger conductor capacity would result only in exceeding the thermal limits of the existing circuits.

As an illustration, Figure 6 shows a typical case of three parallel 22-kv lines serving a 24,000-kw steel-mill load. The reactors in these feeder circuits were originally installed to limit the interrupting duty imposed on the feeder circuit breakers. Two steps of increased capacity were required, first about a 50 per cent increase to a load of 36,000 kw, and later an increase to more than double, or approximately 70,000 kw. In carrying out these steps, the procedure outlined above was actually reversed because of the presence of the reactors in the original feeder circuits. Figure 7, therefore, shows the first step, which consisted of building a new circuit of 350,000 circular mil of copper to operate without any reactor and in parallel with the three existing circuits. As a result of this arrangement, the new circuit, both from the standpoint of thermal limit as well as actual impedance, had a capacity of approximately $1^1/_3$ times that of the old circuits, and thus provided the necessary 50 per cent increase in over-all capacity.

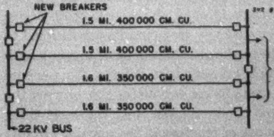

Figure 8. Final layout to serve 70,000-kw load

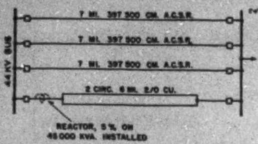

Figure 9. Unequal impedances of lines in parallel—resulting in bottleneck

The final arrangement of the setup, to increase the capacity to approximately 70,000 kw, was then obtained as shown in Figure 8. Here the reactors in the original three circuits were removed, new circuit breakers of adequate interrupting capacity were installed, and the existing number 4/0 copper line conductors were replaced with 400,000 circular mil of copper. Both the breakers and line conductors removed in this process were used elsewhere.

In this case at least three new circuits of the same capacity as the original circuits would have been required to handle the final load increase so that the method of changing conductor size resulted in a net saving of two circuits with all the terminal equipment, line construction, and so on, pertaining thereto. At the same time, voltage regulation under the final setup was quite satisfactory.

2. In other cases of relatively short heavily loaded lines, it may not be feasible to change existing conductors; for example, where steel tower lines are already carrying the maximum physical load. Such a situation may be further aggravated by the necessity of taking a longer route for new circuits.

Figure 9 shows a typical instance where one number 2/0-copper double-circuited line constituted a bottleneck when operated in parallel with other circuits of larger conductor size. Here the installation of a reactor in the number 2/0 copper circuits eliminated the bottleneck and allowed all of the circuits to carry load commensurate with their actual thermal limitations. At the same time, with the fairly short lines involved, no voltage regulation problem was introduced by the use of the reactor.

Medium Length Lines—Both Thermal Limits and Voltage Regulation Involved

In some situations, it may be necessary to increase the capacity of an existing system, consisting of a number of circuits of medium or small conductor size, which cannot readily be changed for physical and economic reasons. Because of increasing right-of-way difficulties, as in the cases mentioned above, it is often necessary that any new circuits constructed to relieve the old circuits be of longer length which immediately defeats the purpose of using larger conductor size or even the same conductor size. At the same time, the use of reactors in the existing circuits would be undesirable from the standpoint of voltage regulation as well as from the standpoint of the number of circuits involved.

For a typical situation of this kind, shown in Figure 10, it was desired to construct two new circuits of relatively high capacity to operate in parallel with four existing circuits of medium capacity, but right-of-way conditions necessitated a longer route for the new circuits. A solution was found in the use of a series booster transformer of simple design connected in each of the two new circuits and operated as part of the line. Using a 60-degree series voltage, obtained from an adjacent phase of a wye-connected transformer as in the zigzag transformer connection, these transformers introduced both a voltage boost and a phase-angle shift to force the line to carry a heavier share of both kilowatts and reactive kilovolt-amperes. The desired results in loading, as shown by Figure 10, were obtained with only $2^1/_2$ degrees angle shift and the corresponding voltage boost of about $2^1/_2$ per cent, which required a series winding of only five per cent. Such an installation, even allowing for a maximum angle of five degrees for future contingencies, can be made relatively inexpensively, as it does not require additional switching facilities or a transformer designed for changing taps under load. Taps, of course, should be provided, but can be changed by momentarily de-energizing one circuit at a time. The amount of phase angle required is so small that it should not be necessary to remove it under light load conditions.

Longer Subtransmission Lines—Voltage Regulation Controlling

In some situations, the length of lines and loading is such that voltage regulation rather than thermal limitations is the determining factor. If the system is laid out with sufficient circuits to give reliable loop service, the use of some means for voltage regulation is probably the most efficient and economical way of increasing system capacity. If not too many stations are involved, individual regulation such as load-ratio-control transformers might be used. Where the need for regulation is quite extensive, it may be more economical and otherwise more advantageous to install one or more synchronous condensers for power factor and voltage control. The beneficial effect of synchronous-condenser operation, particularly if installed at the proper load point on the system, extends over the entire system and even permeates all the way back through the transmission and generating systems. The increase in capacity provided thereby is usually greater than that obtainable from other types of voltage-regulating devices.

If systems such as these are traversed by high-voltage transmission lines, transformer installations tapping directly to the transmission lines may offer the most economical means of increasing the system capacity and at the same time solving the voltage-regulation problem. Even with a synchronous condenser installation, the problem remains of providing the necessary transformer capacity at the transmission substations or other points of supply for the particular system involved.

Increase in Capacity by Revised Concepts in Operating Practice

In addition to the ways and means discussed above for obtaining increased capacity in subtransmission systems by actual physical changes in these systems, there is also the possibility of getting more out of existing systems merely by a change in prevailing conceptions as to what constitutes feasible operating practice. In other words, during the present emergency situation, it may become necessary to revise existing standards as to the combined requirements of service continuity and voltage regulation. For example, in cases

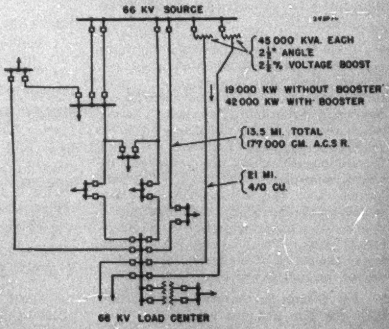

Figure 10. New circuits with larger conductor but greater length compensated to pick up load

where thermal capacities have not been reached but where voltage regulation is the limiting factor, it may be quite proper to dispense with the common criterion of being able to handle the peak load within prescribed regulation limits with one line out of service, and rather to load all of the lines to the point of permissible regulation and accept an emergency reduced voltage if one line goes out. This, of course, cannot be carried to an extreme such as where the load is supplied by only two lines, since the loss of one such line would result in practically no service at all. It should be feasible, however, where three or four circuits are operating in parallel. Such a situation, of course, is not the most desirable and should be avoided if possible but it may be a necessary sacrifice during the emergency to dispense with the luxury of perfect voltage regulation under all expected line-outage conditions.

In other cases, where thermal capacity limits do enter into the problem, it may be necessary to allow an appreciable encroachment on the normal thermal limits on a short-time basis during the outage of one line, so that a larger loading can be carried with all of the lines in service. In defense of such loading, it may be pointed out that line outages of long duration are relatively infrequent and, furthermore, the simultaneous occurrence of a long-time outage during both a high ambient temperature and a low wind-velocity condition would be relatively improbable.

5. Conclusion

It is realized that only a few of the many possibilities that exist for more efficient handling of the problems of power supply to distribution substations have been touched upon in this paper. It is obvious that for such ideas to be utilized at their fullest potentialities, a substantial background of system planning and studies is needed. But even in such cases knowledge of possibly new but tried methods of approach should help.

It is hoped that the suggestions and examples which have been given in this paper will be of value not only as direct suggestions but also indirectly as an incentive to further thought and discussion on this important subject.

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Distribution Substations and Wartime Necessities

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Synopsis: Requirements of war will impose unprecedented demands upon many distribution substations. Equipment will be overloaded before extensions can be built, if at all. New construction must be simplified to the bare essentials. Substitute materials must be used. Protection and maintenance must be intensified and operating personnel must be educated to new responsibilities and skills. These are discussed and means are suggested to meet these needs.

LIMITED to that very important part of the whole power system indicated by the title, that is, the "distribution substation," it is the purpose of this paper to make suggestions, to stimulate thought, and to invite discussion of means available under wartime conditions to serve increased loads, to preserve and protect existing equipment, to provide for restoration of service when outages occur, and to employ the available materials, equipment and man power economically in the necessary extensions to existing substations and in the construction of necessary new substations.

The authors do not here presume that they have exhaustively treated the subject, and they confidently expect that in addition to their suggestions many other means will occur to engineers and operating men and will be developed from time to time under the varying requirements of individual situations.

More Intensive Use of Existing Equipment

The three principal functions for which distribution substations are established are: transformation, voltage regulation, and switching. Usually, apparatus for all these three, together with accessories such as line entrances, potheads, cables, lightning arresters, busses, instruments, meters, and control equipment are grouped together in assemblies of varying capacity and complexity, depending upon local needs and the preferences of designers and users.

Bottlenecks limiting the load which may be carried from a given substation may appear in many items of the equipment mentioned. Their correction or removal often can unlock substantial increments of vitally needed capacity.

TRANSFORMERS

In general, power companies do not load main power transformer banks to the limit of their capabilities. However, as loads continue to increase during this emergency, in advance of the possibility of obtaining equipment, many transformers will have to operate far above manufacturer's rating.

The idea that a transformer name-plate rating is a limit beyond which load must never go dies hard. Transformers may be operated without distress at loads well above kilovolt-ampere ratings upon the basis of temperature. There is considerable material published on this subject which can be re-examined with profit at this time.[1–4] To carry load on transformers beyond the limit fixed by temperature under self-cooling, some form of artificial cooling must be used. For this purpose, artificial air circulation and water jets are most common. Under emergency conditions, almost any form of fan will circulate the moderate volume of air required if there is a sufficient number of these fans properly disposed to reach the greater proportion of radiating surface of the transformer. The use of water jets is more effective than air circulation but is, of course, confined to those locations where supply of water can be made available. Forced circulation of the insulating oil through separately-mounted radiators or water-cooled heat exchangers is also possible. This scheme is particularly adaptable to indoor installations. By use of these methods of cooling, loadings may be increased, often 50 to 200 per cent over name-plate kilovolt-ampere rating, depending upon the daily load cycle, prevailing ambient temperatures, the scheme of artificial cooling adopted, and liberalness of design of the transformers themselves.

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VOLTAGE REGULATORS

Voltage-regulator loadings may be increased in precisely the same manner as transformer loadings, except that in step-type regulators, the tap-changing switching equipment must be considered. In some instances, induction-type regulators having series parallel windings can be doubled in current capacity by connecting windings in parallel but at a sacrifice of half the range of regulation obtainable when operated in series. It has been too often observed that the "bucking" half of the available regulating range is unused. In such cases, this reduction of range will entail no hardship if the bus voltage is adjusted on a scheduled cycle. Booster transformers may be installed in conjunction with regulators connected in this manner to maintain voltage at locations where its level at peak otherwise would be objectionably low.[5]

CIRCUIT BREAKERS

Increases in current-carrying capacities of circuit breakers can be obtained in some instances through silver-plating of contacts and bushing studs, and by frequent careful inspection and adjustment to assure maintenance of contact pressure. Switching arrangements can be adopted which place two circuit breakers in parallel, but care must be exercised that the impedances of parallel leads are equal, lest the resulting current unbalance overheat contacts and other current-carrying parts of one of the breakers.

When adding to transformer kilovolt-ampere capacity, either by replacement of an existing bank with larger units or installation of additional units, the increase of transformer capacity may increase fault current sufficiently to exceed the interrupting capacity of the circuit breakers. In such a case, if the substation has two or more transformer banks installed, the main bus may be separated into two or more sections, thereby limiting fault current to a value within the capability of the circuit breakers. This method introduces an element of inflexibility into the operation of the substation and to that extent may be objectionable, but it may be the only practical solution to meet the situation at hand. Another solution is to arrange to trip one or more of the transformer breakers or a bus-sectionalizing breaker a few cycles ahead of the feeder breakers in cases of high-current faults, thereby reducing the current to be interrupted by the feeder breakers.

Reactors may be inserted between bus sections normally fed from different transformer banks. A method of limiting short-circuit current applicable where bus sectionalizing is not possible is the insertion of series reactors in the supply circuits. Such reactors will introduce additional regulation and losses into the supply circuits. This effect, where objectionable, may be overcome by normally short-circuiting the reactors through a breaker or fuses co-ordinated with the time characteristics and interrupting capabilities of the feeder breakers.[6] Series reactors may, in emergencies, be "homemade" and constructed from materials that are stock items, that is, bare copper cable and concrete.

If a circuit breaker of adequate current-carrying and interrupting capacity is available, it may be used with appropriate relaying as a group breaker to open a group of feeder circuits when fault current exceeds the interrupting capacity of individual feeder breakers. The analysis of individual situations will often indicate that the impedance of only a few hundred feet of overhead line is required to reduce the fault current to a value which can be safely interrupted by the feeder breaker. By carefully adjusting relay settings, unnecessary operations of the group breaker can be held to a minimum and consequently only a very small percentage of the total tripouts will involve interruptions on feeders in the group which are not in trouble.

Finally, in some cases, the risk will have to be assumed that such faults as do occur will not be of maximum magnitude, and hence breakers will continue to be used at locations where their interrupting ratings are exceeded. This forced decision will unquestionably be reached frequently in the days ahead.

It seems hardly necessary to point out the increased need for thorough and frequent inspection of circuit breakers during this emergency and of keeping them in even higher state of operating efficiency than has been the practice in the past. When apparatus so necessary for protection of vital equipment becomes difficult, if not impossible, to replace, its preservation assumes great importance and reasonable added expense to assure that preservation is warranted.

OTHER SWITCHING EQUIPMENT

Air-break switches and disconnecting switches, particularly those of earlier manufacture, may have capacities increased through the silvering of contact areas and increase of contact pressure. In some cases, this may involve the use of clamps installed over the jaws of contacts. However, it is fortunate that this apparatus as installed is generally of liberal current rating and is seldom a bottleneck in the system.

CABLES AND DUCT LINES

Forced ventilation of cable tunnels, manholes, and duct lines may be required to reduce ambient temperatures and increase capacity of cable circuits. Such

Figures 1–3. Suggestions for release of needed substation equipment

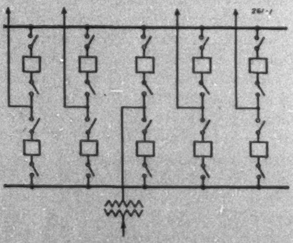

Figure 1. One-line diagram of a double-bus substation

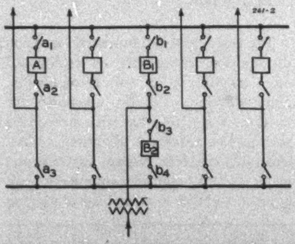

Figure 2. One-line diagram of double-bus substation converted to main- and inspection-bus arrangement releasing four oil circuit breakers and four sets of disconnecting switches

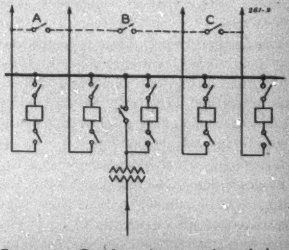

Figure 3. One-line diagram of single-bus substation showing portion of distribution circuits to illustrate sectionalizing and cross-connecting of these circuits to give advantages of inspection bus

Parallel facilities together with such sectionalizing facilities as are usual and desirable for transfer of load and isolation of faulted sections are provided at points A, B, and C ventilation must be directed so that, in case of failures, fires will not be spread to other parts of the substation or operators driven away from their posts of duty.

Busses and Connections

Drastic restrictions in use of copper have been imposed by the Office of Production Management. Copper as a conductor of electricity is recognized as necessary, and power companies undoubtedly will continue to obtain the metal, but in reduced quantities and after much delay. This delay may even become the equivalent of no deliveries at all in the cases of special sizes and shapes of bars and fittings.

Busses and equipment leads are usually of ample current-carrying capacity without exceeding the usually accepted temperature rise of 30 degrees centigrade. This rise, however, may be materially exceeded without deleterious effects on buses, leads or equipment attached to them. Increase of current in the bus of the order of 20 per cent above the limit fixed by present standards seldom will raise bus temperature more than an additional 10 degrees centigrade, if there is reasonably adequate opportunity for dissipation of heat. Higher operating temperatures may require additional provision for expansion, some contact surfaces may require silver-plating to reduce local heating, and soldered joints may need to be replaced by brazed or welded joints. Forced ventilation may be required to protect adjacent apparatus and cables from heat generated within enclosed bus structures. In special cases, temperature of bus bars may be lowered where one section of the bus carries all or a large proportion of the total current, through rearrangement of incoming and outgoing circuits to reduce the current carried in any one section.

Obtaining Additional Circuits and Releasing Switching Equipment

Many substations are in operation which have duplicate equipment not essential to normal operation. Some of this may have to be "borrowed" for more active duty elsewhere. This often can be accomplished without introducing serious operating handicaps. Figure 1 and Figure 2 illustrate one example of this procedure. Figure 3 shows how, in some instances, this may be carried further, still retaining reasonable operating flexibility.

Where duplicate bus and switching facilities are used primarily to permit regular maintenance work on the usual 40-hour-week basis, the rescheduling of this work on a three-shift 24-hour-day basis will often permit these changes and simplifications.

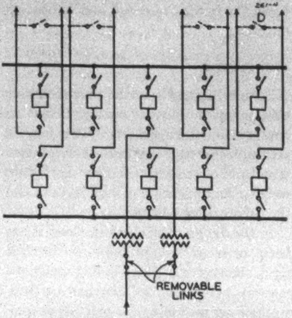

Figure 4. Suggestions for doubling number of circuits and transformer capacity with no additional switching equipment

One-line diagram of double-bus substation of Figure 1 converted to a dual single-bus substation with doubled transformer capacity and number of circuits

Parallel facilities similar to those shown in Figure 3 are provided at point D

Interrupting duty on oil circuit breakers is not increased by this increase in capacity

Figure 4 illustrates a means of doubling the number of circuits and the transformer capacity of the substation of Figure 1 without either increasing the number of breaker positions or the interrupting duty on the breakers.

Automatic high-speed reclosing has been advanced in recent years as a working substitute for duplicate service. Where this would result in saving new construction in the distribution system, the rebuilding of older low-speed breakers may be avoided in substations arranged as in Figure 1 and Figure 2 by the following relay scheme, reference being made to Figure 2. To effect this operation, an auxiliary relay is connected to the trip circuit and auxiliary switches of breaker *A*, such that completion of the opening cycle of breaker *A* energizes the closing circuit of breaker B_2. With disconnecting switches a_1, a_2 and a_3 and b_1, b_2, b_3 and b_4 closed, tripping of breaker *A* will then automatically close breaker B_2 and reenergize feeder *A*.

Static Capacitors

Where static capacitors are installed on distribution systems, consideration should be given to their proper location on feeder circuits for relief of substation equipment and apparatus that may be severely overloaded.

Installation of capacitors at substations, although not usually as productive of over-all system economies as when installed near the load centers on feeders, may still relieve overloaded condensers, transformers, subtransmission circuits, and system generating equipment. Switching groups of capacitors to control the reactive load and bus voltage can effectively take the place of synchronous condensers or extend the regulating range of existing synchronous condensers.

Customer Co-operation

The co-operation of power customers in scheduling as much of their operations to off-peak periods as practicable offers possibilities for releasing capacity in all current-carrying facilities. There are many industries and commercial enterprises where this off-peak use of power can be effected without difficulty and others where it may be accomplished after careful study and planning. It seems likely, in the emergency, that such co-operation will not be too difficult to obtain where off-peak operation is at all practicable. Co-ordination of the maintenance schedules of customers and power companies should be developed to minimize interference with wartime production and the burden on reserve facilities.

Equipment Capabilities

Operating men will be able to meet load changes and utilize the maximum capabilities of equipment only by keeping themselves more than ever intimately informed of load conditions and the actual capabilities of equipment available, and by using utmost ingenuity in applying that knowledge. Transformers, regulators, circuit breakers or other equipment may become heavily loaded at one substation, and replacement by larger more lightly loaded units from another substation may be indicated. However, only through a thorough knowledge of the equipment itself and what it may be expected to do, can the operating man know whether or not the transfer is justifiable. By keeping information up to date, the need for rearrangement may be anticipated, planned in advance, and changes effected with a minimum of delay.

Equipment and materials in stores should be inspected and, if necessary, reconditioned for immediate use. Thereafter, these should be maintained in readiness in the same manner as those already in service. Many units of equipment and items of material that in more normal times would be looked upon as obsolete or unsuitable for further use can be reused, either by the company, a sister company, or a customer. It is a reasonable prediction that reserve equipment will be depleted and older equipment outmoded or formerly considered inadequate will be found highly useful in the days ahead.

INTERCOMPANY CO-OPERATION

Consideration should be given to the exchange of information among power companies as to the availability of equipment that may no longer be needed for use by the one, but invaluable perhaps for service by another. There should also be merit in a tool-lending and exchange service, similar to that which is understood to be in operation among some of the Canadian utility companies, and in expansion of such service to include replaceable equipment and materials.

Power companies, in the past, have co-operated fully in times of need, and in this emergency period the utmost co-operation is required that all may together better perform a difficult job.

Economies and Conservation in New Work

PLANNING AND DESIGN

In the design of substations to serve wartime loads, a very large "factor of ignorance" must be dealt with in the preliminary stages. Experience has indicated that actual demands may vary up to several hundred per cent from preliminary figures.

As scarcity and priority restrictions both operate to make materials and equipment difficult to obtain, initial orders should cover the full requirements of the work. To run short of materials needed to finish construction may seriously postpone operation of the facilities. To insure against omission of materials required for complete construction, plans must be prepared in greater detail than heretofore considered necessary. This greater detail will also assist erection in view of the lesser skill and experience of the available labor. Extreme care should be exercised in obtaining accurate material lists from the plans. Construction work must be carried out with fidelity to plans, as small changes may call for materials not on order and procurable at best only after long delay.

The scarcity of competent men applies with double force in the engineering and drafting groups, where the detailed planning of new work is done. Simplification of layout, use of standard or semistandard groups of equipment, and elimination of "special" items are all essential to keep down the burden of detail and to prevent mistakes.

STRUCTURES

Structural steel is, at the time of writing this paper, a shortage material, and its conservation is imperative. This may be accomplished through use of substation structures of greater flexibility and lighter weight, simplification of bus layouts and the substitution of wood.

In the design of steel substations, it has been more recent practice to provide rigid structures. This rigidity could be relaxed materially in distribution substations by providing for only slack-span attachments of lines to structures without sacrifice of needed strength or safety. There are many earlier structures of this lighter design in service throughout the country which continue to stand as evidence that these lighter, more flexible structures are adequate.

Wood can be substituted for steel in the majority of jobs. Where insulation is equal to that which would be used on steel structures, and where hardware is thoroughly bonded and grounded, wood structures are electrically equal to steel. Many wood structures long have been in every-day operation. There is no question but that they can be built to operate satisfactorily for all usual voltages, will last through the war period, and will conserve materials sorely needed elsewhere.

BUS AND CIRCUIT ARRANGEMENT

Many of the plants recently established for producing war material provide for power supply to at least half of the load from each of two sources, including separate substations. The practice of providing transfer-bus, inspection-bus, or other duplicate-bus arrangements for new distribution substations should be abandoned for the duration of the conflict in the interest of conserving needed materials and equipment. To serve loads for which separate feeds are already provided, this practice is inexcusable. To serve loads not vital to prosecuting the war, in view of the impelling needs of the times, the omission is amply justified. Substations, if expected to be permanent, may well be designed to permit future elaboration and installation of additional facilities when conditions will permit.

For high-voltage moderate-current bus bars and connections, structural steel shapes or steel pipe may be substituted for copper tubing or bar so long as copper continues to be a shortage material and more difficult to obtain than steel. It may be necessary to use copper cable as bus in lieu of copper tubing or bar, as it seems likely that copper in such form will be more readily obtainable and requires less labor for production and installation. Busses may be reduced in cross section at a saving of material through design to operate at higher current density and through reducing bus current by locating the supply transformer connections as closely as possible to the load center of the substation bus.

FUSES IN LIEU OF BREAKERS

Fuses can be substituted for high-side breakers in substations of simple layout. One precaution may be needed, that is, phase-failure relays may be needed to open the low-side breakers to prevent damage to customers' polyphase equipment in case the supply to the high-voltage side is single-phase due to blowing of one fuse. With this precaution and with proper fuse co-ordination, the installation of high-side breakers may be eliminated, and indeed has been eliminated under normal practice in many situations with satisfactory results.

INSULATION LEVELS

Insulation levels and insulation co-ordination should not be sacrificed in the case of simplified substation layouts. The cost of protecting service by providing adequate insulation strength is very little compared with that of duplicate facilities, throw-over switches and other provisions, which are usually installed to permit continued service in case of apparatus failure. In one case, the cost of the next higher level of impulse insulation in an important unit of substation equipment accounted for only five per cent in the cost of that one item. This might well be considered as representing the insurance necessary to permit elimination of permanently installed duplicate or spare equipment with accompanying switches, bus, and cables.

TRANSFORMERS

The power companies which in the past made a consistent effort to standardize transformer voltages and taps are in position to reap large benefits from this past effort during the present emergency. One of the advantages of this standardization is, of course, that such companies are in position to "play checkers" with their transformers, moving them about between substations to meet the shifting demands. In this connection, it would be well to emphasize the economy of buying new transformers only in the larger sizes in current use by such a company and installing these at locations where smaller transformers are overloaded so that they in turn can be released for installation in new substations.

The three-phase transformer is more economical than three single-phase units of equal rating from the standpoints of material and labor that enter into its manufacture. It requires a single foundation of lesser volume, shorter and more simple bus connections, and fewer man-hours of labor to install. The modern three-phase transformer is reliable in operation and its use in substations of moderate capacity should be carefully considered as a measure of material and labor conservation.

New substation transformers, whether single-phase or three-phase units, should be obtained with blowers or at least with provision for their addition later. Additional capacity may be obtained in this way at a very small cost per kilovolt-ampere, and the economics of losses and regulation can well be neglected "for the duration."

Unit Substations

The limit to which substation capacity may or should be expanded at a given site should be very carefully considered with respect to the cost in dollars, materials and man power of transmission and distribution facilities immediately associated with it. The development of the factory-built single-unit-type substation consisting of transformer, regulator, circuit breaker, and auxiliaries in a single case offers a very attractive means of increasing capacity to serve a given area without adding to either feeder capacities or substation capacities at an existing site. These unit substations are particularly adapted to installations where it is necessary to reinforce feeder circuits by installing such units close to the load and breaking up heavily loaded feeders to keep load within the capacity of existing substations and their outgoing circuits. The fact that such unit substations may be installed quickly and moved quickly to new locations is a decided advantage in meeting unexpected shifts or additions to loads.

Unit substations are economical in material and labor required in manufacture and require less material and labor of erection for foundations, structures, and bus work than equivalent conventional substations. Their use is of advantage also in that labor required for installation may be largely of lesser skill.

During the trying days ahead, the use of unit substations may prove to be worth while even in instances where such use in more normal times might not be considered economical. Many of the substations erected to serve war loads or loads incidental to war activities will be idle after peace returns. Salvage of a conventional substation involves a considerable write-off, whereas the salvage value of a unit substation is high, there being little unrecoverable material and equipment, and a minimum of labor is required to move it to another point of use and to place it in operation there.

Restoration of Service

Reserve Equipment

Difficulty of obtaining priorities for additional equipment and the continuing growth of load will inevitably bring about active use of much of the reserve equipment of power companies. Restoration of service after equipment failure will then present a different problem than when spare facilities were more plentiful. It has been rather general practice to provide a spare transformer at every important substation. With increasing need for transformer capacity to carry the growing load, and the impossibility of obtaining new transformers in any reasonable time, it is likely that more of this reserve equipment will be placed in operation. It may then be that one transformer will have to serve as a spare for several transformer banks, probably at substations located some distance apart. Such a spare unit should be centrally located with respect to the substations where it may be required for service and kept in readiness for transporting and placing in service in minimum time. Skids should be in place and the unit cribbed to a convenient height for loading on the transporting vehicle. Flexible leads connected to the bushing studs and provided with clamps at the free end will save time in placing such a transformer in operation promptly. Other heavy equipment reserved for use as spares should be similarly prepared.

Transportation facilities for moving a spare transformer are, of course, necessary but there seems to be little justification generally for permanent mounting of spare transformers on special trailers. Transformers of modern design are reliable, and it does not appear that the delay occasioned in loading will justify the cost of transportation equipment that would be reserved for a single service and would remain idle over extended periods of time. In these times, we must recast our notions of what is necessary and what may be only desirable.

Portable Substations

In this period of emergency, portable substations in general do not seem to offer outstanding advantages as compared with separate units of spare equipment. Portable substations must be used as an entity and cannot be separated so that each component part can operate apart from the others. With spare equipment at a premium, it would seem to be the better policy to have spare units of equipment so that these units can be used at different locations as needed. For example, a spare transformer can be used in one substation and at the same time, a spare circuit breaker used for emergency service at a second substation. There may be certain systems where portable substations will have advantages over separate units of equipment, but in the majority of instances during the present emergency at least, spare units of reserve equipment offer a greater degree of protection to service in having a wider diversity of use, although the time element for restoring service may, in some cases, be slightly in favor of the portable substation.

Storage of Reserve Materials

Spare parts and materials held as reserves against substation outages may in some instances be stored at the substation of probable use or they may be stored in a central location. In the first case, the number of spare parts and quantities of materials required will be greater than would be necessary if centrally located. Central storage is preferable in offering the maximum availability of minimum quantities of reserve materials and in many cases permitting better maintenance and care. With either plan, spare equipment should be covered by careful up-to-date inventories both as to specifications and locations.

Protection and Preservation of Equipment

Inspection and Maintenance

Greater attention must be given to the operating equipment to observe or anticipate distress and thereby forestall failures in service. Equipment should be inspected more frequently and maintained at a higher degree of precise operating condition than has in many cases heretofore been considered adequate. Moreover, when inspection reveals the need for even minor adjustments, repairs, or replacements, they should receive appropriate attention at once. Failure to act in these minor instances does not necessarily mean that the equipment will fail to continue to operate satisfactorily, but it may and certainly will in some instances shorten the period before major replacements will be required. It should be kept in mind that an ounce of preventive maintenance is worth a ton of replacement, especially when replacement is beset with present uncertainties.

Fire Protection

With the prospect of continuing intensive operation of equipment, it is not only vital to preserve that which we have but to protect it from loss. Fires are not frequent but when they occur in a substation, the damage to the equipment where the fire originates is all too often extensive. Moreover, except as precautions have previously been taken, damage to adjacent equipment is the rule. Any fire, even though it does no major damage, diverts man power from the primary function of operation and absorbs materials needed elsewhere. Often the incidental damage to other equipment and the interruptions to service necessary to clean up and make final repairs are of greater consequence than those involved in the immediate failure. Consequently, careful consideration should be given to protection against fire hazards.

Probably the most economical and effective protection available at this time against fires resulting from oil ignition in outdoor substations is obtained from water spray apparatus. Its use is, of course, limited in application to those locations where water may be made available. Transformers installed at important substations where water can be obtained may be protected by fixed or portable spray nozzles or by a combination of the two. Perhaps the most satisfactory over-all method is the combination of the fixed and portable apparatus. A moderate number of fixed nozzles properly installed will tend to prevent the spread of fire and reduce its intensity until such time as portable apparatus can be brought into action by trained men.

Where water is not available, dry-compound apparatus is quite satisfactory as are carbon-dioxide extinguishing devices, although at present the latter are costly and not always effective for outdoor use. Carbon-dioxide apparatus has its widest application indoors and for this use is most satisfactory in that there can be no deterioration of equipment resulting from its use.

Foam is effective for extinguishing oil fires under some circumstances but its use around electrical equipment is open to objection because of the work required to remove the caked foam after the fire is out.

Arrangements should be made for co-operation with local fire departments in the study of fire-fighting problems, in co-ordinating power company fire-fighting equipment with the department apparatus, and in joint drills and rehearsals. The most important element in minimizing damage is the adequate training and drilling of personnel in the proper use and limitations of use of whatever apparatus may be brought into action.

Lightning Protection

There is considerable divergence of opinion as to the need for lightning arresters, particularly for voltages above 66 kv. Experience of many operating companies in numerous widely separated parts of this country and Canada, under diverse operating and climatic conditions, has established the value of spill-gap protection for substation apparatus.[7] The valve action of the lightning arrester, by limiting the time and magnitude of power-follow current, reduces the number of interruptions from lightning surges, but evidence is lacking that the lightning arrester protects substation equipment more effectively than the spill gap. Conservation of material and labor dictates that serious considerations be given to the use of spill gaps in place of arrester installations at new substations or where replacement of arresters may be necessary.

Personnel Training

Of equal importance to protective equipment in the protection of property is a trained staff of operating men who in case of trouble or incipient trouble know what to do, how to do it, and can act promptly as a well-co-ordinated team. The time taken and money spent to educate and continue the education of these men in this important function will be amply justified. In view of conditions that confront the industry, each one should be induced to feel the added responsibility resting upon him. He should be instilled with an enthusiastic desire to do his full part in providing and maintaining the power supply for war and should be thoroughly trained to do that part well.

Conclusion

Difficult problems will arise in the days ahead due to scarcity of materials, equipment, and suitable man power, which problems must be solved by the engineer and operator in the struggle to serve the rapidly expanding war loads. The manner in which these men meet their more unusual problems will be of interest to all who have similar problems to solve. It will be of benefit if these solutions are passed on to the industry as a whole through publication currently in the technical press.

Experience may indicate that some of the needful practices of rigid conservation established during the present emergency will earn recognition as standard practices in the peaceful days that are to come.

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Overhead Distribution Systems in Wartime

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Synopsis: An overhead electrical system makes use of large quantities of materials which are also very vital to the nation's wartime program. This paper seeks to point out the most practical ways of conserving these vital materials, thereby releasing them in greater quantities for use in wartime production. In general, this can best be accomplished by keeping to a minimum the quantities used to make the line extensions and system reinforcements which will be required to supply electric service to the new industries and military establishments, and to the increased housing facilities which must accompany them. A number of effective tools are available which may be utilized to reduce the quantities of conductor material required to give satisfactory service. There are also possibilities of rearrangement of existing facilities so as to use them more effectively.

Critical Materials Involved

THE critical materials which are used to the greatest extent in the construction of overhead distribution lines are copper, aluminum, steel, and zinc. Of these copper is by far the most important. Aluminum has been making considerable headway in recent years as a conductor material when provided with a steel reinforcement, but, while it has many good features to recommend its use, particularly for long span rural lines, competing materials of copper-steel composite have been made available which can be used to build these lines. The fact that aluminum has been practically unobtainable for some time was not a serious handicap as long as we were still able to get copper-steel composites. These materials, however, are now being greatly restricted in their availability, and the problem, so far as copper is concerned, is, therefore, to find ways to minimize its use, since no substitute material other than aluminum is available. Various possibilities of accomplishing this purpose will be discussed later.

Steel is used in considerable quantities in overhead-distribution-line construction, principally for line hardware and fittings. It would be difficult to find a substitute material which would be nearly so suitable for such important items as bolts and nuts. For certain uses, however, such as insulator-pins, crossarm-braces, platforms, and other fixtures requiring a considerable proportion of the steel tonnage used in overhead-lines construction, wood is quite a satisfactory substitute.

Zinc is important in overhead-lines work, mainly as a coating for steel to prevent rust. The tonnage used for galvanizing of line hardware is not very great, however, and it is to be hoped that its use, particularly for bolts, nuts, and so forth, may be continued. On the materials of larger size it will be possible to use coatings composed of less vital materials which, while less permanent, will be effective through the present emergency and can later be replaced with more lasting materials.

The need for materials, other than metals, used in the construction of distribution lines is not immediately critical but there is a good chance that it may be unless their consumption is curtailed. Poles and crossarms are the most important of these items. Due to increased consumption of these materials in connection with military and emergency housing, stocks of seasoned material have been seriously depleted, and it is reported that unfavorable weather conditions in pole-producing areas have resulted in a subnormal output of new poles, especially in the larger sizes. Conservation in the use of poles and the avoidance of large stocks not immediately required will help keep to a minimum the amount of substandard materials used in the construction of lines which will need early replacement.

Means of Conserving Materials

In general, the conservation of vital materials can best be accomplished in four ways:

1. By using the minimum possible amount of material on all new line extensions.
2. By using the minimum amount of vital material for bolstering up existing lines when overloaded or under-voltage conditions result from increased loads.
3. By rearranging existing facilities so as to use them more effectively.
4. By salvaging needed materials from existing lines where they are not immediately required to give adequate service.

New Line Construction

The most effective way to save material is, of course, not to use it. To the extent, therefore, that restrictions are applied to the extension of lines to serve potential consumers, there will be an almost proportionate reduction in the amount of material used for overhead-line construction. This is for the reason that in periods of expansion the greater proportion of material used goes into the construction of lines to serve new areas. Unless a real effort is made to concentrate housing facilities in compact groups, this will still be the case. It is not within the scope of this paper to discuss the restrictions to be placed on new line construction by those in control of the allocation of material. Of one thing we are certain, and that is that extensions will be made to serve new projects vital to the emergency effort. It would seem almost equally imperative that the necessary extensions be made to serve new housing facilities which are, in a great many cases, essential to the war effort.

In the building of such new lines as are permitted, every effort should be made to keep the material used to a minimum. The most obviously beneficial expedient is to use the least possible amount of copper. The size of the wire used should be the minimum to give reasonably satisfactory service to the initial customers, unless increased loads are known to be coming in the immediate future. For most distribution engineers this will be a reversion to obsolete practices, since it is the way things were done before the adoption of system-planning methods. We can well remember the frequent and costly reconstruction, replacement, and revamping of lines which were necessary in those days. It must be admitted, however, that building to an ideal ultimate plan sometimes results in the installation of line capacities which are much more than adequate for a large part of their useful life when load growth is slow. In the light of present shortages, we would be justified in returning to the old-fashioned method of using the smallest size of wire which would do the initial job, even if, in some instances, it may require changing to a larger size in a year or two.

It may also be possible to justify some modifications of the pole layout so as to conserve material. Temporary short-cut routings of line extensions may be resorted to in some cases to reduce the

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length of line required. The use of longer spans in the initial stages of residential development will conserve poles, crossarms, hardware, and insulators. The ultimate spacing of poles in residential areas is invariably fairly close, because of the necessity of keeping customers' service drops reasonably short, but, if poles can be so spaced that the later installation of intermediate poles as the area builds up results in a good layout, an initial saving in material can be made. In many cases, where the building-up process is not rapid, an over-all saving in investment results. The use of bare primary wire, if permissible, will favor the adoption of this "double-span" construction.

INCREASED LOADING OF EXISTING FACILITIES

The second objective of the conservation program, namely, the devising of ways and means of carrying increased load on existing lines and equipment with the minimum use of vital material offers the greatest opportunity to the distribution engineer for contributing to the war effort. As loads increase on the electrical distribution system, two limitations operate to cause need for increased capacity in lines or equipment. These are:

(*a*) Excessive heating which may cause damage or possibly failure.

(*b*) Excessive voltage drop which may result in decreased efficiency of utilization equipment and even make its operation impossible.

The current-carrying ability of wires and line devices is affected by many widely variable factors, and, in general, the commonly accepted current ratings are arbitrarily conservative. In arriving at these ratings, a combination of conditions is selected which very rarely occurs in practice, and tests made under actual field conditions show that loads considerably in excess of these ratings may be carried for long periods without resulting in any appreciable damage to overhead lines and associated equipment.

Limitations to Wire Loading. The heating of overhead conductors, for example, is greatly affected by air movement, and yet the limiting ratings are often based on temperature rises in still air with high ambient temperatures. As there is practically always some air movement past conductors in overhead-line construction, the effect is to very materially reduce the actual temperature rise. Furthermore, even at the assumed limiting temperature, the effect on the wire or equipment would be serious only if continued for long periods of time. Very seldom will a condition hazardous to service or persons result.

Limitations to Transformer Loading. The loading of distribution transformers is a good example of the ability of equipment to successfully carry so-called overloads. This subject has been discussed at considerable length in recent technical publications* and will be treated only briefly here. The factors which make it possible to carry peak loads greatly in excess of the name-plate rating are:

1. The thermal capacity of the iron core, the insulating oil, and the tank, absorb heat from the windings and greatly reduce the resulting temperature rise on short-time peaks.
2. The loads of highest peak values and longest duration usually occur in winter when ambient temperatures are low and the resulting temperature in the windings is correspondingly reduced.
3. The limiting temperatures assumed in the rating would cause failure of the transformer only after continuous operation over a period of years.

The greatest problem in the proper loading of distribution transformers, of course, is to know what the character of the loads is on individual transformers, both as to yearly peak value and as to the shape of the load curve throughout the year. However, the general characteristics of residential loads are sufficiently well established that, with any one of several methods of determining transformer peak loads, it will be conservative to allow estimated winter peaks of the order of 150 per cent of name-plate rating. Under this procedure, experience shows that transformer life should be reduced very little, if any, over that which would be obtained by more conservative loading. However, before proceeding to materially increase transformer loading beyond this point, it is particularly important to have adequate information on load characteristics and values, since a large number of damaged transformers on a system would be a serious detriment to good service in later years.

Limitations to Voltage Regulation. Excessive voltage drop, causing wide variation in voltage regulation to customers, will probably be a more frequent cause for system reinforcement than current limitations. Here, again, it would seem that present accepted good practice could be modified somewhat during the emergency. Fairly narrow ranges of voltage regulation may be largely justified by economic considerations in normal times, although by the same consideration we cannot afford to maintain as high standards on rural lines as in thickly settled urban districts. When, in the face of serious material shortages, economics is no longer the ruling consideration, it would seem that standards of voltage regulation which apparently are giving satisfactory service to the farmers should not be too poor for the city dweller to put up with for the period of the emergency. This should apply, not only to the long-time variations, but to the sudden fluctuations commonly known as "flicker."

Improvement of Power Factor. In spite of increased current ratings and more liberal voltage limitations, the time will be reached, as loads increase, when something will have to be done to bolster up the supply. But, this need not always take the form of adding new facilities or replacing old ones with like kind of larger capacity. One of the most promising expedients which is available, not only to improve voltage regulation on overhead lines but to decrease the line current capacity for the same load, is the use of shunt capacitors to improve the power factor of the load. An extensive use of this device has been justified under the normal economic conditions of the past few years, and it offers a very effective means of conserving vital material under present conditions. It is true that capacitors require for their construction an appreciable amount of aluminum foil, but the amount of copper which could be saved by their intelligent use should recommend them very highly to the authorities having to do with the allocation of all of these materials.

The use of shunt capacitors will find its best application in the densely loaded parts of our systems, where the reduction of current will often be as important as the reduction of voltage drop in the circuit. In rural areas where conductors are small, loads are light, and power factors high, shunt capacitors are not quite so effective a remedy, although they may still be justified in many cases. The effect of the capacitors is to cause a constant voltage rise in the circuit which will, if too many capacitors are installed, cause too high voltage at light-load periods unless provisions are made to cut out portions of the capacitance. So far, no economical method has been devised for this purpose which is applicable to small installations.

Series capacitors should also be useful devices for improving the capacity of long lines serving individual customers, although they will not have as wide an application as shunt capacitors by reason of the lack of flexibility in their application.

Voltage Regulation and Boosters. Another very economical expedient available to improve the regulation of long rural circuits is the use of line-type voltage regulators or step-voltage boosters. These are now manufactured in a wide range of sizes and steps of voltage change. The simpler forms of step-voltage boosters are quite satisfactory for rural or suburban line regulation, and their use produces an improvement of voltage regulation which would require many times the amount of material in conductors to produce like results.

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* See list of references at close of paper.

Standard distribution transformers, if they have the proper turn ratio, may be used very effectively as a booster to produce a better range of voltage in a circuit at a very low cost. A small amount of fixed voltage boost at a point fairly well out on the circuit will result in a considerable improvement in peak load voltage to the customers on the tag ends without causing too high voltage at off-peak periods. Booster-transformers connected to give a larger amount of voltage boost, used in connection with automatic regulators to permit the better use of the full range of the regulator, will also prove to be a useful expedient where conditions are favorable. For a slight increase in cost, standard transformers may be purchased with increased secondary-to-ground insulation which will insure their satisfactory operation as booster transformers.

Rearrangement of Existing Facilities

In any well engineered electrical-distribution system there will always be an appreciable amount of wire and equipment which will be capable of carrying much greater loads than they are at the time called upon to carry without exceeding either current or voltage limitations. This is partly the result of the planning of system development to follow a layout of facilities which will be adequate to serve an area after it has become completely built up without too much replacement and rearrangement of the initially installed material. A critical shortage of materials, however, may make it desirable to do a certain amount of rearrangement of these materials at this time, so as to use them more effectively and thus reduce the amount of new materials required. Line wire and transformers are the items of overhead-line construction which offer the best possibilities of such treatment.

REMOVAL OR REPLACEMENT OF WIRE

Expedients to conserve the use of conductor material which should be given most consideration are as follows:

1. Replacement of the larger sizes of wire with smaller in locations where the load has not yet developed or has been greatly reduced by system rearrangements, and the transfer of the larger wire to locations where the load is increasing rapidly. This will in turn release, for the relief of other circuits, an amount of wire nearly equal to that originally used, if proper salvage methods are used to prevent the scrapping of any large part of the material in the process.
2. Building new sections of line which will shorten feeds to increasing loads rather than replace or add wire on existing routes. This will frequently be practical in suburban or rural areas where piecemeal extensions often result in a poor layout. In many cases, when this is done, sections of line of lengths equal to that installed can be salvaged.
3. Removal of wire which is not required to maintain service except in case of the failure of some other source of supply. Throwover connections, ring feeds, stand-by lines are frequently installed to improve reliability of service to customers. Perhaps, if material shortage becomes really critical, a lowering of service reliability standards would be justified, and some of these facilities could be removed in situations where no consideration of public safety is involved.

Since most of these expedients will release more material than they will require, it is hoped that some method will be found to make it possible either to secure blanket approval of their use or to set up some very informal means of securing approval of individual projects. Otherwise, the volume of paper work and the delays entailed in securing approval may be so great as to seriously discourage any attempt to adopt them. This is particularly true in the case of overhead-lines work where individual projects are small and their number very large. The volume of paper work necessary for specific approvals could easily become so great as to seriously interfere with more important projects.

CONSERVATION OF TRANSFORMER CAPACITY

In parts of many overhead-distribution systems, the ratio of demand on a circuit to the total installed transformer capacity on the circuit will be found to be low. This would seem to indicate a large surplus of transformer capacity, which it should be possible to utilize to serve new loads, and reduce, if not eliminate, the necessity of purchasing new transformers for some time. Actually, the possibilities of doing anything very helpful with this situation are not very good. In rural or suburban areas, one of the locations where this ratio of circuit demand to installed capacity is very low, the widespread use of electric ranges, refrigerators and other types of appliances causes high short-time load demands which make it necessary to provide larger transformers than would otherwise be required, in order to give satisfactory voltage conditions. Also, the demands on the individual transformers are not coincidental, and, because of this diversity, the demand on the circuit is much less than the sum of the demands on the individual transformers. The ratio of the circuit demand to the total installed capacity is, therefore, not a proper measure of the loading of the individual transformer and the surplus capacity is more apparent than real.

In urban areas also, during the initial stages of development, this ratio of circuit demand to transformer capacity is low. It might be thought that, in this case, an increased spacing of transformers would reduce the number required with no increase in initial size, but very few liberties may be taken with the spacing of transformers without producing intolerable voltage conditions. Larger secondary wire would be of very little help in this connection and its use would only increase the use of vital material. It would also be unwise to go too heavily into the use of very small transformers for the initial stages of urban area development. The reduction in vital material would not be anywhere near the ratio of the reduction in size, and, in the future, it would probably be impossible to find economical use for them anywhere on the system. Indeed, many of us, with the extensive development of rural lines in our territories, are already having difficulties in finding use for the smaller transformers removed from the farm lines by reason of increased demands of customers on these lines requiring replacement with transformers of larger capacity.

It should be possible to utilize some of the surplus transformer capacity already installed by making interchanges of transformers as some become loaded up and others do not. This will be particularly true if full advantage has not already been taken of the possibilities of loading transformers to the limit of their thermal ability. By this means it should be possible to reduce appreciably the total kilovolt-amperes of transformer capacity required on the system. It will still be necessary to buy new transformers if extensions are made to new areas since, for the reasons given above, it will be difficult to release any large number of transformer units from existing lines.

Salvaging Operations

In addition to conserving new materials for line construction, a great deal can be done toward the same end by the maximum use of material removed from the lines, of which there is usually a considerable amount in any large organization.

Highly profitable salvage operations have already been organized in a number of companies for this purpose, and in the present emergency it is imperative that this practice be adopted by all operating utilities.

Salvage operations which are most productive in the conservation of line materials are the following:

1. Cleaning, straightening, and splicing together into standard lengths all wire suitable for reinstallation.
2. Removing weatherproof covering from wires not suitable for reinstallation, and splicing in lengths suitable for recovering or use as bare wire.
3. Cutting up very short lengths for use as tie wires, ground-rod pigtails, and so forth.
4. Reconditioning hardware, fuse carriers, lightning arresters, and all articles of line equipment not hopelessly obsolete.
5. Cutting off rotted sections of poles and retreating them for use as shorter poles or guy stubs.

Conclusions

It is to be hoped, of course, that some of the expedients suggested in this paper will not be necessary as they involve burdensome operating costs and much additional labor. In some localities now, and probably generally later on, labor will be difficult to obtain. Other expedients involve a lowering of standards of service to our customers and should be adopted only in moderation unless the situation becomes quite critical, and then only with the approval of the authorities charged with regulation of service standards. Failure, however, to adopt some of the practices, which are in the interest of the conservation of materials and are at the same time favorable to the economical design and operation of the electrical system, would be called unprogressive at any time. In the present national emergency, it should be considered unpatriotic.

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Variable-Speed Drive for United States Army Air Corps Wind Tunnel at Wright Field, Dayton, Ohio

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THE machinery manufacturers are being required to solve many design and production problems in connection with our national-defense program. All electrical manufacturing companies are exerting maximum effort to produce generators, motors, conversion apparatus, and other electrical equipment which are needed in meeting the requirements of a large and rapidly expanding industrial activity. There is a further need for machines of greater capacity, new combinations of apparatus and control devices, and more information on machine characteristics to accomplish new and difficult objectives.

The new wind tunnel of the United States Army Air Corps at Dayton, Ohio, which is scheduled for operation in the early part of 1942 is an example of this type of problem. Wind velocities of 400 miles per hour and greater are to be obtained for testing large size airplane parts and models. The air is to be circulated by two propeller assemblies mounted on a common shaft and operated in series to produce the necessary high pressure. The propeller assembly is driven by a 40,000-horsepower, variable-speed, wound-rotor-type induction motor, which has a top rotational speed of 300 rpm (327 rpm synchronous speed) and is the largest unit of this type built to date. It is essential in this application to obtain close speed regulation over a wide speed range. This paper discusses the problem associated with the design and operation of the equipment required to meet wind-tunnel specifications. The role which the wind tunnel will play in the race for air supremacy in the present world war is a subject of great interest and national importance, but it will only be discussed briefly in this paper as an introduction to the requirements of wind-tunnel drives.

Wind Tunnels

The principles of aerodynamics and hydrodynamics are neither sufficiently well-known nor subject to sufficiently exact mathematical analysis to permit the accurate calculation of the performance of a body moving through the fluid at high velocities, except for simple cases. It has been the practice of designers for many years to obtain performance characteristic data on equipment and apparatus such as ducts, channels, blowers, waterwheel runners, ship-propeller screws, and ships from the performance of models operated under controlled conditions in laboratories. The same condition is true to an even greater extent in the case of airplanes due to the fact that the plane velocities are extremely high, and materials and structural members must be worked nearer the ultimate limits in order to reduce weights and increase the power per unit of weight. During the early development of the airplane, the Wright brothers used low-power wind tunnels to check plane models rather than resort to the more dangerous and costly method of full-scale flight tests. This condition exists at the present time, and a vast amount of large-scale experimental work will be required to obtain the essential data so that still higher efficiency and flight speed can be reached.

Model tests are quite reliable at low speeds due to the fact that the air-flow lines produced by the full-size unit are similar to those existing in the model test. In some cases, the effects of air viscosity can be determined by varying the density of the air in the tunnel. In other cases, it is necessary to make empirical corrections in extrapolating from the model to the full-size unit. For plane speeds at 400 miles per hour, the air velocity around different parts of the plane may approach the speed of sound. This introduces effects of both the mass and elas-

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ticity of the air. In the present state of the art, large-scale models and full-size parts must be checked in wind tunnels at full speed to get reliable data for building high-power superspeed planes. Fortunately, it is not necessary to use full-size planes, due to the fact that basic data can be obtained for large or full-size parts and the constants for the parts combined for application to the entire plane. It is interesting to note, in connection with wind-tunnel operation, that the power required to circulate the necessary large volume of air around a complete wind tunnel is several times greater than the power required to drive the model through the air at the same speed. Since the power required to circulate the air varies as the cube of the speed, it is not surprising to find that a 40,000-horsepower motor rating is required for the largest high-speed wind tunnel.

Wright Field Wind Tunnel

The Wright Field wind tunnel is designed with a cross section sufficiently large to accommodate full-size plane sections or parts, and large-size complete models, with wind velocities of 400 miles per hour or greater. Since the output from the motor is eventually absorbed by the circulating air, it is obvious that the temperature of the air would continue to rise until the rate of heat loss dissipated by the tunnel became equal to the rate of input to the air, if complete recirculation were used. When operating under full-speed conditions with 40,000-horsepower motor output, the air temperatures would become excessive, and it is thus necessary to resort to partial circulation. Sufficiently large inlet and outlet openings are provided at appropriate sections of the tunnel so that approximately 30 per cent make-up air is provided and the gas temperature kept down to tolerable values. Each of the two stage propellers which circulate the air have 16 wooden blades, which are approximately 40 feet in diameter at the tips. The propeller assemblies, including rotating blades, supporting hubs, and stationary guide vanes, were designed by the Air Corps engineers at Wright Field. The general arrangement of the tunnel and propelling equipment is as shown in Figure 1.

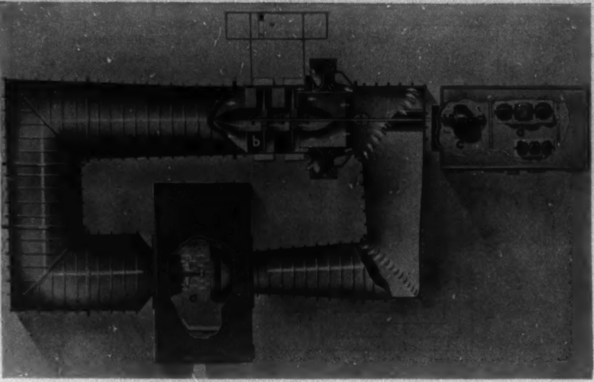

Figure 1. Plan view of wind tunnel and driving apparatus

Driving Equipment

The largest wind tunnel in operation at the time the Wright Field wind tunnel was originally planned, had two 4,000-horsepower driving motors (8,000 horsepower total). A wind tunnel requiring five times as much driving power was a large step forward. It was also desired to use a single driving motor in order to avoid possible difficulties in assembly and complications in the operation of the equipment. Although a 40,000-horsepower wound-rotor induction motor had not been heretofore contemplated, the electrical manufacturers had no misgivings in regard to the feasibility of building it, on account of the fact that synchronous motors on much greater capacity had been built for both 300 and 600 rpm. The motor characteristics—power factor, starting kilovolt-amperes, and speed control—were influenced to an appreciable degree by conditions associated with and requirements of the main power-supply system. In order to maintain speed practically constant at any value over a wide speed range with minimum energy requirements, a speed-control system was chosen which provided for returning the power from the rotor of the main induction motor to the a-c supply system. The main rotating auxiliary equipment required to accomplish this result is shown in Figure 2 and consists of two motor-generator sets. The a-c elements of both sets are salient-pole synchronous units with d-c excitation.

In one set, the a-c motor receives its power supply from the rotor of the main drive motor, and operates as a synchronous machine at the slip frequency over the entire speed range of the main motor. The second set has the a-c generator electrically connected to the 60-cycle main supply system, and operates at constant synchronous speed. Table I shows the speed and frequency for the main motor and auxiliary sets.

Table I

Main Driving Motor				Variable-Speed Motor-Generator Set			Constant-Speed Motor-Generator Set		
Supply Frequency	Rotor Speed	Rotor Frequency %	Rotor Frequency Cycles	Stator Supply Frequency	Rotor Speed %	Rotor Speed RPM	Frequency Cycles	Rotor Speed %	Rotor Speed RPM
60	327	0	0	0	0	0	60	100	600
60	*297	9.25	5.55	5.55	9.25	47.5	60	100	600
60	262	20	12	12	20	103	60	100	600
60	196	40	24	24	40	205	60	100	600
60	131	60	36	36	60	308	60	100	600
60	88	80	48	48	80	411	60	100	600
60	** 37.5	89.55	53.8	53.8	89.55	460	60	100	600
60	0	100	60	60	100	514	60	100	600

*Maximum running speed of main driving motor. **Minimum running speed of main driving motor.

A qualitative interpretation of how the main and auxiliary equipment perform can be readily obtained by analyzing the power, voltage, current, torque, and speed relation that exist under given operating conditions.

Starting and Operation of Equipment

It is apparent by reference to Figure 2 that the unit can be started either from the main driving motor or from the a-c element of the constant-speed motor-generator set. When starting from the main motor, no excitation is to be pro-

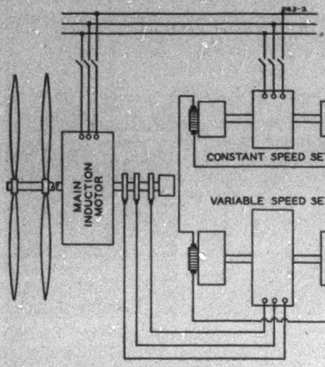

Figure 2 (left). Diagram of equipment and main power connections

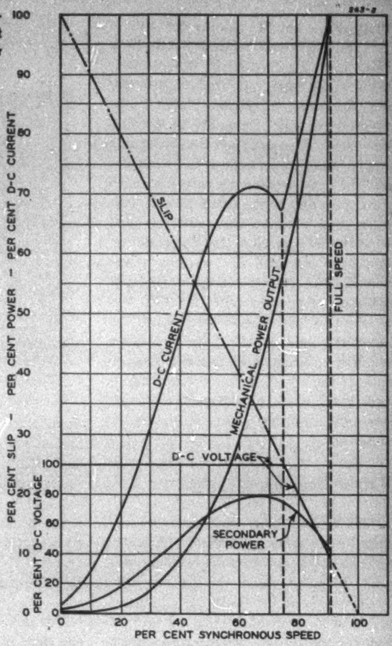

Figure 3 (right). Characteristic curves of a 40,000-horsepower drive

vided for the d-c and a-c elements of the auxiliary sets. When power is applied to the stator of the main motor, the rotors of the main motor and variable-speed set start to roll and accelerate in speed. The speed reached by the rotor of the main motor will be relatively low, due to the fact that the output of the main-motor rotor winding is limited primarily to the losses of the variable-speed set. The speed of the variable-speed set will be relatively high, for its speed is determined by the slip frequency of the main driving motor. If excitation is applied to the field of the a-c element of the variable-speed set, it will pull into synchronism with the rotor of the main motor. The constant-speed set can then be started from either the d-c or the a-c end. If started from the d-c end, it will be necessary to synchronize the a-c end with the main power supply. When the constant-speed set is started from the a-c end, it is necessary to adjust the excitation of the d-c elements of both sets, so that there will be no appreciable interchange of d-c power. When starting the main drive from the a-c end of the constant-speed set, the stator of the main motor should be left on open circuit, the connections for the remainder of the equipment should be normal, and the excitation of the two d-c units adjusted so that the d-c element of the constant-speed set delivers power as a generator to drive the d-c element of the variable-speed set as a motor. Both sets are then brought up to full synchronous speed in the conventional manner. The rotor of the main motor remains at standstill, and the voltage induced in the stator has the same frequency as that supplied to the rotor. The excitation of the a-c and d-c elements of the variable-speed set are then adjusted so that the voltages of the main-motor stator are in synchronism with the voltage of the supply system. When the voltages have the same magnitude, frequency, and have approximately correct phase position, the main-motor supply switch can be closed without the interchange of an appreciable amount of power between the main motor and the supply system.

There is thus no flow of power between the rotor of the main motor and the stator of the a-c element of the variable-speed set, and hence no torque is produced to accelerate the rotor. By increasing the excitation of the d-c end of the variable-speed set, its speed will tend to decrease and its voltage increase so that it functions as a generator and supplies power to the d-c end of the constant-speed set. This power is then returned to the supply system by the a-c element of the constant-speed set. At the instant the speed of the variable-speed set starts to drop, the initial angular displacement between its voltage and the voltage of the main-motor rotor winding produces a secondary current which reacts with the magnetizing flux to produce a motor torque to turn and accelerate the rotor. As the speed of the variable-speed set decreases, the speed of the main rotor increases and both elements maintain synchronism at the slip frequency of the main motor.

Either of the two methods of starting are practical and satisfactory. The method of starting from the relatively small constant-speed set was adopted due to the fact that less complication is involved in the starting equipment and less shock to the supply system occurs.

It is apparent from the above discussion and through a knowledge of the characteristics of a-c and d-c motors and generators that the amount of electric power supplied by the rotor of the main motor and its slip frequency can be determined and controlled by varying the excitation of the d-c elements of the two motor-generator sets and controlling the flow of power between them. The amount of wattless power returned to the supply system can be controlled by varying the excitation of the a-c generator element of the constant-speed set. The amount of wattless kilovolt-amperes supplied to the main motor can be controlled by varying the excitation of the a-c element of the variable-speed set. The curves in Figure 3 show the slip, mechanical-power output, and rotor-electrical-power output for the main driving motor, and the d-c voltage and current of the auxiliary sets as a function of the speed of the main motor.

The elementary power relations in any induction machine are best understood by first considering an ideal machine without loss. In this machine the same torque is developed on the stator and the rotor by the fundamental flux and current. The product of this torque and the rotor speed represents mechanical power output (P_m). The product of the torque and synchronous speed represents power input (P_i), and the secondary power at the slip rings (P_s) is represented by the product of torque and slip speed.

Expressing the slip as a decimal fraction (s) of the synchronous speed, the secondary power can be written in terms of the mechanical power as $P_s = P_m[s/(1-s)]$. For a fixed propeller and a given air density, the power P_m varies as the cube of the speed, which gives a maximum secondary power at $s = {}^1/_3$.

The accurate power relations are obtained by adding the friction and windage losses to the mechanical power (P_m) and subtracting the secondary I^2R from the secondary power P_s to obtain the net power at the slip rings. To trace the power flow from the slip rings through the auxiliary machines back to supply line, it is necessary only to subtract the losses of the various auxiliary machines at each step.

The variable-speed set operating from the slip rings of the induction motor will have a speed proportional to slip. Neglecting the losses, the variable-speed set must handle the secondary power (P_s) which is then equal to the product of its speed and torque. The torque could be written as $P_s/s = P_m/(1-s)$, and from the curves of Figure 3 it will be seen that although the secondary power is maximum at $s = {}^1/_3$, the maximum torque will be at minimum slip. The product of this maximum torque of the variable-speed set and its synchronous speed is approximately equal to the full rating of the main motor, and this determines the size of the variable-speed set. The fact that the maximum current is required only at the minimum speed is of advantage in designing the d-c generators for large capacity.

The constant-speed set must handle the same maximum d-c voltage and current as the variable-speed set, but the maximum voltage occurs at about four times as high a speed requiring a correspondingly smaller machine for the d-c motors. The a-c generator feeding the power back into the line must handle only the maximum secondary power minus the auxiliary machine losses. While the total capacity of the auxiliary sets is great, the speed can be kept high and the physical size is not so large as might be expected.

With the power-speed relations in mind, it becomes apparent that it is desirable to keep full field on the d-c generators over the upper range of speed where the torque is high, and to control the speed by the field of the d-c motors. In the lower range of speed the d-c voltage is held constant and the flux reduced in the generator. The rheostats for the motors and generators are mounted on one shaft, but arranged so that they are changed one at a time.

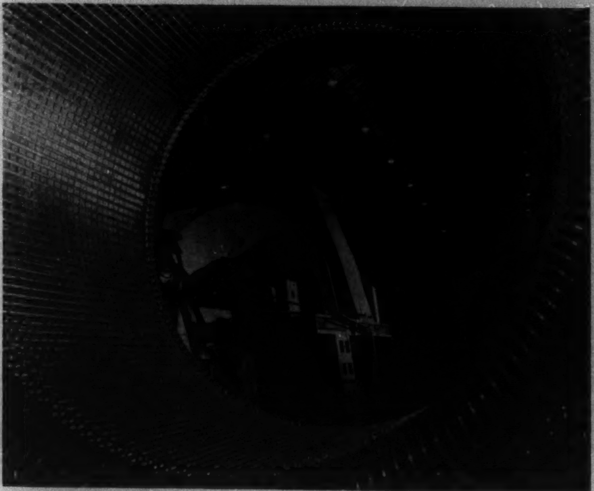

Figure 4. View of main induction motor looking through the stator of the synchronous motor for the variable-speed set

New Problems

In addition to the problems in designing the largest induction motor yet built, there were a number of other problems which had to be solved in the application of this system of speed control. These problems included the steady-state stability, and the dynamic stability or the reaction of the system to oscillating torques either impressed by the propeller or self-excited.

The usual induction-motor criterion of stability that the torque should increase with increased slip is readily met since, even with fair regulation of the d-c machines, the secondary power taken by the variable-speed set, with fixed fields, would increase much faster than the slip frequency. The synchronous motor which is fed from the secondary of the induction motor, must stay in synchronism with the slip frequency, and this presents a stability problem with new conditions. However, it can be shown that the induction motor can be treated as a high reactance transformer with high magnetizing current so that the problem is resolved into the conventional two-machine synchronous-stability problem.

The possibility of self-excited torsional oscillations mentioned above arises from the fact that at low slip frequencies, the reactance-resistance ratio is much lower than in large machines of normal frequency. This tendency to oscillate is the same general phenomenon of hunting encountered in small synchronous machines but is complicated by the addition of the induction motor in circuit. This tendency to establish self-excited oscillations has been termed negative damping, since it is due to a torque in phase with the velocity, or opposite in sign to the usual damping torque. Negative damping may exist in this system for relative angular motion between the induction and synchronous motors. Fortunately, the synchronous-motor damper winding and the speed-torque characteristics of the d-c machines, as well as the fan load, all contribute to the positive damping of the system, so that by proper attention to these factors it is possible to get a net positive damping. Hence it is possible to avoid any tendency to hunt. Methods of analysis of this problem have been written up and proposed for presentation at the winter convention.

A model system was set up using a 100-horsepower wound-rotor motor with two synchronous d-c motor-generator sets. The propeller was represented by a d-c generator with an added flywheel. The starting and control features were tested and the tendency to hunt at low-slip values investigated. With small machines, it was not possible to represent the large units exactly, but the test did serve to prove that the methods of analysis were adequate.

Since there are torsional impulses on the propeller resulting from slight irregularities in the air flow caused by guide vanes, it was necessary to determine their effects on the entire system. The fan, the main induction motor, and the various auxiliary machines, all represent inertias tied together either by shafts or electrical ties. This complicated system has many modes of vibration and many natural frequencies. These were solved by setting up a complete equivalent circuit representing the electrical analogue of the mechanical system, along the well-known principles of representing torques by voltages, velocities by currents, inertia by inductance, torsional flexibility by a capacity, and damping by a resistance. Solving for the natural frequencies of this electrical analogue was difficult due to its many branches. The a-c calculating board was used to represent the system and the approximate frequencies found which could then be checked by direct calculations in complex numbers. The torsional impulses are low, and the net damping is found to be adequate to keep any oscillations low.

The same electrical analogue was found to be very useful in calculating the stability of the speed-regulating system. Since the speed variations must be measured on the motor and the corrections applied through the d-c auxiliary machine fields, the regulating problem is not simple, and careful consideration had to be given to the amplification and antihunting features of the speed regulator.

Conclusions

The type of drive adopted for this largest wind-tunnel application is found to be very efficient to give low starting currents. Also this system lends itself to full automatic control and accurate speed adjustment.

The solution of the many problems associated with this drive have led to some advances in the analysis of machines and in the design of very large motors. This knowledge should prove valuable in the future, on other variable-speed drives, on ship drives and other applications involving systems of machines.

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Progress in Design of Electrical Equipment for Large Diesel-Electric Locomotives

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ASSOCIATE AIEE

DURING the last eight years, as the result of development of the Diesel engine, Diesel-electric locomotives have come into wide use for heavy switching, passenger, and freight service. There has also been a rapid development in the electrical equipment going back about 15 years.

The primary function of the electrical equipment is to transmit the power from the Diesel engine to the rail, and also change the relation of engine to locomotive speed. In modern high-speed equipments, the weight of electrical equipment somewhat exceeds the weight of the Diesel engine with radiators, and so forth, and is of the order of 20 per cent to 25 per cent of the locomotive weight. This may appear to be out of proportion, but it must be realized that the propulsion output of the Diesel engine is all converted to electrical energy and then converted back to mechanical output at the rail over a very wide range of speed.

Modern high-speed passenger and freight locomotives operate under the widest range of conditions. They traverse the plains, rolling country, and mountainous ranges.

Definitions

In order to understand clearly the operating conditions affecting equipment design, it is well to define several items:

1. *Maximum Test Speed.* This is the speed in rpm defined by the rules of the A.S.A. for a stand test of the motor for two minutes and is 120 per cent of the rpm corresponding to:

2. *Maximum Safe Service Speed.* This is the highest locomotive speed permitted in service with minimum size wheels (worn wheels) on down grades.

3. *Balancing Speed on Straight, Level Track.* With normal trains, this is generally somewhat less than the maximum safe service speed, and is of interest chiefly for comparative purposes since stretches of straight, level track long enough to permit reaching balancing speeds are not frequently encountered.

4. *Balancing Speed on Prevailing up Grades.* In rolling country, these grades may be of the order of 0.2 per cent to 0.4 per cent. In hilly country, they may be in the neighborhood of one per cent. In mountainous country, long grades of 1.8 to 2.2 are common and helper grades up to 3.5 exist

5. *Unloading Speed.* This is the speed above which the full power of the Diesel engine is not utilized. It should be sufficiently high so that the full power of the Diesel engine can be used on the prevailing up grades in *each* district.

6. *Continuous Rated Speed.* This is the lowest speed which can be maintained with full engine power for prolonged periods with unworn wheels without exceeding the temperature limits of the motors and generators, and should closely correspond to the speed with maximum train weight on the ruling and helper grades unless the time spent on these grades is fairly short and is preceded by a run at light load or a very definite and effective cooling period.

7. *Maximum Tractive Effort.* This is the maximum tractive effort which can be developed for short periods during accelerations. In high-speed passenger locomotives it may be limited by the commutating capacity of the generator, otherwise by the slipping point of the driving wheels. In very powerful freight locomotives, driver slippage is the usual limit, but the strength of freight car draft gear may make the practical limit lower than the slipping point.

8. *Maximum Weight Per Driving Axle at Rail.* This is the weight as limited by stress in rail and depends on the minimum (worn) wheel size and maximum safe service speed.

Chronology

Prior to 1927, standard 600-volt self-ventilated railway trolley-type motors, designed for suburban and subway service, were used. About 1928, motors designed primarily for operation from limited power supply were introduced. These power plants were of 300- to 400-horsepower capacity, with one generator and two motors. In 1932, 600-horsepower equipments were introduced using

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semiforced ventilated motors, class B insulation; with multiple windings and four brush holders instead of the conventional two-circuit winding and two brush holders; with wedges on the core, nonmagnetic bands on the end windings and with roller bearings. In 1936 a further increase to 900-engine horsepower was made. At present, motors are available which can use 1,500-engine horsepower per pair of motors. These motors are only 17 per cent heavier than the motors used with a 400-horsepower engine in 1928.

Motors

DESIGN

This increase in capacity has resulted from improvements which may be listed:

1. Increased peripheral speed of armature. The armature peripheral speed has been increased from 8,500 to 12,500 feet per minute. This has been the result of improved methods of insulating and wedging the coils in the armature slots and better methods of banding the end windings with predetermined band tension and multilayer nonmagnetic bands of high-tensile strength.

2. Increased peripheral speed of commutator; from 7,000 to 10,000 feet per minute. This has resulted from the more detailed study of stresses in the commutator copper and steel parts and of the characteristics of mica, the use of an increased number of brush holders to keep down the over-all length, the seasoning of the commutator and the use of better grades of carbon brushes.

3. Forced ventilation. A definite supply of ventilating air from a source relatively free from snow, water, oil, brake-shoe dust and dirt of all kinds is provided.

4. Better insulation and processes in applying the insulation. Mica tapes and wrappers with a lower per cent of combustible material, glass, and asbestos are used. Coils pressed to exact size exclude air and give high percentage of copper in the coil with good heat conducting characteristics and avoid shrinkage and consequent loosening in service. Also improved sealing compounds exclude oil, moisture and air, thus permitting higher temperatures. It is also good practice to connect all motor and generator field coils on the negative side to reduce the voltage stress in the field coils.

5. Micarta wedges which are capable of standing higher temperatures.

6. Nonmagnetic high-strength multilayer bands on the end windings. In addition to permitting higher peripheral speeds, the elimination of magnetic bands on the armature core and end windings reduces the commutating voltage materially.

7. Multiple armature windings permit higher speeds with low commutating voltage.

8. Armature coils are designed to reduce eddy current loss.

9. Roller bearings.

10. Better gearing.

OVERLOAD AND COMMUTATING CAPACITY

With the increase in capacity, the loss in per cent of input is lower; however, with better ventilation the loss in watts per motor is increased. Since the motor weight has not materially increased, the thermal capacity in proportion to motor capacity has materially decreased. Hence much greater care must be used in applying motors to a given service to avoid overloads that produce high temperatures, which by forcing expansion and contraction will damage insulation. This is particularly true of armature windings which are much less free than field windings to expand and contract. When the motor is operating at its highest ampere load, the speed is low and also the commutating voltage is low. Its overload capacity is determined by heating. When the motor is operating on weakest field with full engine power and highest voltage, its commutating voltage is highest.

Generators

MAIN

The capacity and speed of the main generators is determined by the capacity and speed of the engine. The generator differs from the motor in its speed, mounting and overload capacity. When the generator is operating at its highest ampere load, full speed is obtained, hence its maximum capacity is determined by its ability to commutate heavy ampere loads as well as by heating. Compensating windings, often used on high-capacity generators, are not generally used on railway Diesel-engine generators. This is due to the space taken and the higher resistance of such windings. The main-pole air gap is tapered to reduce the maximum volts between bars and also to reduce the load core loss.

Mechanical connection of the generator armature direct to the engine shaft gives a substantial rigid connection using the weight of the generator armature in place of an engine flywheel. The generator stator is also usually direct connected to the engine frame.

AUXILIARY

It has become general practice to drive auxiliary machines such as compressors, radiator fans and main motor fans, and so on, from the engine. An auxiliary generator need be of only small capacity, sufficient to charge battery and supply power for headlights, cab lights and miscellaneous small auxiliaries.

Control

The differential exciter of special design has been developed to give full engine loading over a wide range of locomotive speed under usual conditions. To provide for unusual conditions and to secure the very maximum utilization of engine power, the field of the exciter is put under control of the engine governor. When fuel rack exceeds a predetermined setting, the exciter field is decreased, and when the fuel rack goes below this setting the field is returned to its normal maximum value. This avoids overload on the engine and keeps the fuel rack at full load without sacrificing the inherent advantages of the simple differential exciter. By operating on the exciter field, the physical size of the device is kept low. Since the time constant of the exciter field is much more rapid than that of the main generator field, this does not slow up the operation.

Conclusion

For present high-speed passenger locomotives, electrical equipment is available with a weight between 20 and 21 pounds per engine horsepower, with continuous rated speed 33 per cent of maximum service speed of above 100 miles per hour and with sufficient maximum tractive effort to slip wheels at 33 per cent adhesion. For freight locomotives with double the number of motors per power plant, the weight is between 30 and 31 pounds per engine horsepower, and the continuous rated speed 16 per cent of maximum rated speed.

Reference

ELECTRICAL APPARATUS FOR DIESEL CARS AND LOCOMOTIVES, G. F. Smith. AIEE TRANSACTIONS, volume 55, 1936, April section, pages 334–41.

Distribution-Type Lightning-Arrester Performance Characteristics

AIEE COMMITTEE ON PROTECTIVE DEVICES

Lightning Arrester Subcommittee*

THIS report presents the results of work by the lightning arrester subcommittee to bring up to date and make available to the industry the performance characteristics of present day valve-type distribution arresters having a maximum rating of 3 kv to 15 kv. Distribution arrester characteristics were given in a report by the lightning arrester subcommittee in ELECTRICAL ENGINEERING, volume 56, May 1937, page 576. Since that date there have been changes in the arrester. The subcommittee felt it desirable to present up-to-date values and also to include values at higher discharge currents than those previously given.

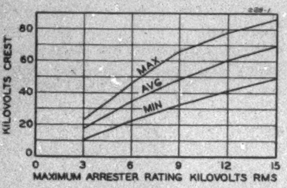

Figure 1. Arrester impulse-gap breakdown, AIEE wave

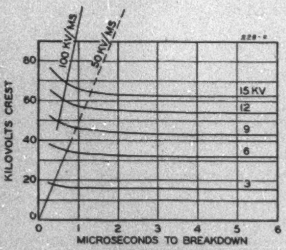

Figure 2. Arrester impulse-gap breakdown (average values)

Table I includes the arrester gap breakdown values—maximum, average, and minimum—from tests on the arresters using the rates of voltage rise specified in the Lightning Arrester Standards Bulletin 28, ASA Standard *C-62*. Table I also shows maximum, average, and minimum *IR* discharge voltages for all arresters at discharge currents of 1,500, 3,000, 5,000, 10,000, and 20,000 amperes, all on a 10x20 current wave.

To enable rapid interpretation, the data have been plotted in curve form.

Figure 1 shows the relationship of arrester gap breakdown for the AIEE rate of voltage rise versus arrester maximum rating, with maximum, average, and minimum values.

Figure 2 shows arrester gap breakdown for other rates of voltage rise as well as for the AIEE standard rates. The breakdown values cover a range of 0.25 microsecond to 6 microseconds.

Figure 3 shows the spread found in *IR* discharge voltage values for a discharge current of 1,500 amperes, for the several voltage ratings.

Figure 4 shows average *IR* discharge voltage for the different discharge currents from 1,500 amperes to 20,000 amperes and the different voltage ratings 3 to 15 kv.

Four lightning-arrester manufacturers supplied the data from which the characteristics in the report were tabulated and plotted. The values are for arresters of present day manufacture and do not necessarily apply for older types.

While the total spread for all manufacturers is of the order of plus or minus 40 per cent, the tolerance permitted by any one manufacturer does not exceed plus or minus 25 per cent for arrester gap breakdown voltage, or plus or minus 20 per cent for arrester *IR* discharge voltage from the average performance values which are published in his literature.

The 60-cycle spark potential of the arresters referred to in this report will not be less than 150 per cent of the arrester maximum line-to-ground rating.

By comparing the volt-time characteristics of the insulation to be protected with the volt-time characteristics of the arrester, an evaluation can be made of the margin of protection.

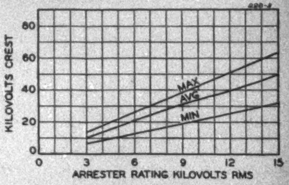

Figure 3. Arrester discharge voltage at 1,500 amperes

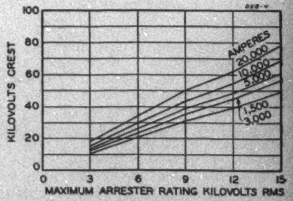

Figure 4. Arrester discharge voltage on 10x 20 current wave (average values)

Table I. Performance Characteristics Distribution-Type Arresters (3–15 Kv)

IR Discharge Voltage on 10x20 Current Wave

Arrester Maximum Rating (Kv)	Arrester Breakdown AIEE Wave†			1,500 Amperes			3,000 Amperes			5,000 Amperes			10,000 Amperes			20,000 Amperes		
	Min.	Avg.	Max.	Min.	Avg.	Max.	Min.	Avg.	Max.	Min.	Avg.	Max.	Min.	Avg.	Max.	Min.	Avg.	Max.
3	10.5	17.4	23	7	10.7	14	7.9	12.5	16	9.4	13.7	18	10	15.5	21	11.7	18	24
6	22.5	35	46	12.7	21	27	14.5	23.6	31	15.9	26.1	34	19	30	38	22.5	34.4	44
9	32.2	48.7	66	19.7	31	39	22.3	35.5	45	24.5	39	51	28	43.9	57	33	50.5	66
12	41.3	60.6	78	25.9	39.4	51	29.4	44.7	58	32	48.8	62	37.5	54.9	69	43.5	62	77
15	49	69.1	86	31.5	40.5	63	35.6	56.4	71.3	39.2	61	77	45	69	85	53	78.6	99

†The AIEE wave is 50 kv per microsecond for arresters rated 3 and 6 kv and 100 kv per microsecond for arresters rated 9, 12, and 15 kv.

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* Personnel of the AIEE lightning arrester subcommittee: H. W. Collins, *chairman;* H. N. Ekvall, I. W. Gross, Herman Halperin, F. M. Defandorf, Edward Beck, W. J. Rudge, J. R. McFarlin, A. H. Schirmer, J. M. Towner, H. R. Stewart, E. R. Whitehead.

Hot-Spot Winding Temperatures in Self-Cooled Oil-Insulated Transformers

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In recent years operators have wanted to know how much overload a transformer would carry for a given length of time without exceeding a safe temperature. This knowledge would enable them to obtain the most from available equipment or to install equipment which would provide reasonable overload capacity in case of emergency. In order to do this, it is necessary to be able to calculate the temperatures within windings, and to know safe operating temperatures.

It is not easy to calculate the temperatures within windings, and rough rules which are extremely conservative have been used. For example, it was recognized that there were places in the windings hotter than the average temperature, and an arbitrary 10-degree hot-spot allowance was made.

It is shown in this paper, that the hot-spot allowance for many transformers is less than five or six degrees centigrade, and that the gradients do not increase as fast as previously supposed, due to the effect of decreased oil viscosity at higher oil temperatures. This makes it possible to recommend much higher emergency overloads than in the past. In an example it is shown that 200 per cent load could be carried for one hour, without exceeding what appears to be a conservative temperature limit, as against previous recommendations of 138 per cent load for the same duration of time.

Variation of Temperature With Load

In presenting this subject, it is thought that it will be easier to follow if we take a specific transformer as an example and carry through calculations for a given overload. Comparison of the calculations and actual test results will show whether the method is accurate or not.

Test Data

Full-load heat run, 2,000-kva single-phase transformer, 44,000 high voltage, 2,300 low voltage, 60 cycles.

Actual copper loss	9,450 watts
Iron loss	5,200 watts
Total losses	14,650 watts
Ambient temperature	27.5 C
Rise by resistance,	
High voltage, winding	34.8 C
Low voltage, winding	33.8 C
Top oil rise	30 C

An overload run was made at 135 per cent load. The copper losses at the 100 per cent run were at an average copper temperature of approximately 34 degrees centigrade rise, plus 27½ degrees centigrade ambient, or 61½ degrees centigrade. If the winding on the 135 per cent run was at about 75 degrees centigrade, the copper losses would be:

$$\frac{9{,}450\times 1.35^2\times(234.5+75)}{234.5+61.5}=18{,}000 \text{ watts}$$

The total losses would then be 18,000+5,200 or 23,200 watts. If the oil rise at full load was 30 degrees, the oil rise at 135 per cent load should be $30\times(23{,}200\div 14{,}650)^{0.8}$ or 43.4. This is based on the assumption that the hot oil rise above ambient varies as the 0.8 power of the total losses. The gradient between the high-voltage average copper and the hottest oil is 4.8 degrees centigrade, at full load. If the gradient varies as the 0.8 power of the copper losses, the new gradient should be $4.8\times(18{,}000/9{,}450)^{0.8}$ or 8 degrees centigrade. This would give an average copper rise of 43.4+8 or 51.4 degrees centigrade.

The actual temperatures at 135 per cent load were 43 degrees centigrade oil rise, and 46.6 degrees centigrade copper rise, compared to 43.4 degrees and 51.4 degrees centigrade, respectively for calculated values.

In order to check this further, several transformer test records were examined where overload runs had been made, and the results are tabulated in Table I, below.

The interesting part of this tabulation is found in the fact that the oil rises seem to follow, within a few degrees, the values obtained by calculation, but it will be noted that there is a considerable discrepancy in the gradients. In general, they change much less than might be expected. It will be noted that so far as the temperature rises at overloads are concerned, values calculated in this way from full-load data are apt to be higher than those obtained in actual runs.

There are several reasons why these approximate methods give the results they do. The laws of convection and radiation of heat losses from the tank surface and radiators seem to be well established, and the average temperature rise above ambient air varies very closely as the 0.8 power of the losses. This would apply with a small error to both the hottest and the average oil, if the difference between top and average oil is not great.

For example, suppose we have an average-oil rise of 26 degrees centigrade, and a top-oil temperature rise of 30 degrees centigrade, at full load. We can refer

Table I. Overload Heat-Run Data

Transformer Test	Load	Copper Losses	Total Losses	Copper Rise by Resistance H.V.	Copper Rise by Resistance L.V.	Top Oil Rise Actual	Top Oil Rise Calculated	Gradients Average Copper to Top Oil, Actual H.V.	Gradients Average Copper to Top Oil, Actual L.V.	Gradients Average Copper to Top Oil, Calculated H.V.	Gradients Average Copper to Top Oil, Calculated L.V
A	100	9,450	14,650	34.8	33.8	30		4.8	3.8		
	135	18,000	23,200	46.6	48.1	43	43.4	3.6	5.1	8	6.4
B	100	5,300	7,565	25	25.5	20		5	5.5		
	125	8,550	10,815	31.9	34.9	26.5	26.7	5.4	8.4	7.3	8.1
	135	10,180	12,445	36	40	32	29.8	4	8	8.4	9.3
C	100	7,120	10,404	31.3	34.5	26		5.3	8.5		
	125	11,400	14,684	40	41.9	33.3	34.2	6.7	8.6	7.7	12.4
	135	13,530	16,714	43.9	44	37.5	38	6.4	6.5	8.9	14.2
D	100	10,800	14,508	31.5	33.8	25		6.5	8.3		
	125	17,620	20,840	41.5	43.3	34.5	33.4	7	8.8	9.6	12.3
	135	20,450	23,670	47.2	47.9	39	37	8.2	8.9	10.8	13.8
E	100	6,650	9,350	30	15.5	22.5		7.5	—7.5		
	125	10,520	13,220	40.7	28.4	29	29.6	11.7	—0.6	10.8	*
	135	12,900	15,600	45	34.7	34	34	11	+0.7	12.7	*

*Not calculated because of apparent "negative" gradient.

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The authors acknowledge the help of C. W. Penney, Westinghouse research department, in the preparation of this paper.

back to the first example given. The calculated top-oil temperature rise using the 0.8-power rule at 135 per cent load is 43.4 degrees centigrade, and actually was 43 degrees centigrade. If the same rule is applied to the average-oil rise, which is 26 degrees centigrade, at full load, the calculated value would be 37.6 degrees at 135 per cent load. The difference between the top-oil and average- or effective-oil temperature is assumed to be 4 degrees at full load. Suppose it varied anywhere from 4 to 6 degrees at the overload. The calculated top-oil temperature rise would range from 41.6 to 43.6 degrees versus 43 degrees test. This indicates that where the difference between top oil and average oil is not great, both of these rises may be assumed to vary as the 0.8 power of the losses.

If we follow along this reasoning, the difference between the average copper and average oil adjacent to the coils will be 8.8 degrees centigrade at full load, and 14.7 degrees centigrade at 135 per cent load. This assumes that the gradient, copper to oil, varies as the 0.8 power of the losses. The difference between the top oil and average copper might then be, at 135 per cent load, 14.7−6.0 degrees or 8.7 degrees centigrade, compared to 3.6 degrees centigrade measured. This indicates that there are other factors than those considered, or that the factors used are in error. These factors are oil flow or convection, and the effect of changes in oil temperature on the gradients between the copper and oil.

Effect of Convection Currents

The convection currents within windings and in the radiators are not easily estimated from the dimensions, and no effort to provide methods for such calculations is to be made in this discussion. However, it is possible to consider the variation of convection currents and their effect on temperature rise. Table I contains some interesting material. For example, there are variations in the test gradients of the low-voltage and high-voltage windings at different overloads which do not look consistent. Part of this may be due to experimental error. The corrections for "time to shut down" were made according to AIEE test standards. The largest factors in these variations are undoubtedly the difference in length and area of the oil paths through different windings and the variation in the watts loss per-unit area in the windings. This is particularly true in the case of the last unit. The low-voltage winding has a relatively large area of ducts, and a very low value of watts per square inch, compared to the high-voltage winding. The result is that the oil flows more easily through the low-voltage winding and is relatively cooler when it leaves the low-voltage coils than the oil leaving the high-voltage winding. If the watts per square inch of coil surface on both windings were the same and the length and area of the oil paths were the same, then both windings would behave more nearly alike.

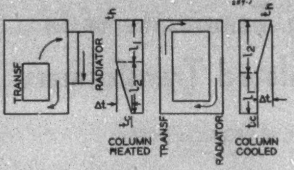


Figure 1. Sketch showing main convection current in transformer and equivalent circuit, with oil temperatures at various points

If both windings are about alike, it is possible to replace them thermally with a heating and cooling circuit as shown in Figure 1.

The density of the oil at $t_h = D_{t_h} =$

$$\frac{1}{(1+0.00074\,\Delta t)}\,D_{t_c}$$

The pressure of the liquid in column 1 will be

$$l_1\left(\frac{D_{t_c}}{1+0.00074\,\Delta t}\right)+ l_2\left(\frac{D_{t_c}+\dfrac{D_{t_c}}{1+0.00074\,\Delta t}}{2}\right)$$

and in column 2

$$l_2\left(\frac{D_{t_c}+\dfrac{D_{t_c}}{1+0.00074\,\Delta t}}{2}\right)+l_1 D_{t_c}$$

The difference between these quantities represents the "head" available to cause oil flow, or

$$l_1 D_{t_c}-l_1\left(\frac{D_{t_c}}{1+0.00074\,\Delta t}\right)=H$$

$$H=l_1 D_{t_c}\left(\frac{1+0.00074\,\Delta t-1}{1+0.00074\,\Delta t}\right)\alpha K\,\Delta t$$

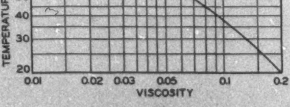


Figure 2. Variation of kinematic viscosity of transformer oil with temperature

The conclusion can be drawn that the pressure head causing thermosyphon flow is practically proportional to the difference between the hot and cold oil temperatures. Although this is derived for a specific case, it is generally true as long as the temperature gradients in each leg remain proportional.

If the flow is slow enough, and is streamline in character, not turbulent, the rate of flow is proportional to the head, and inversely proportional to the viscosity. This can be expressed as—

$$\text{Rate of flow}=K\frac{\Delta t}{v}$$

Also the losses which are dissipated are equal to the rate of flow times the specific heat (S.H.) times the temperature difference—

$$\text{Rate of flow}\times K_2\times(\text{S.H.})\times(\Delta t)=\text{losses}$$

$$\therefore\ \text{Losses}=K\,\frac{\Delta t}{v}\times K_2\times(\text{S.H.})\times\Delta t$$

$$\therefore\ (\Delta t)=K_3\sqrt{\text{losses}\times\text{viscosity}}$$

Figure 2 shows the relationship between oil temperature, in degrees centigrade, and the kinematic viscosity of a transformer oil. In order to show the effect of these factors, a transformer was tested at overload, and top and bottom temperatures noted, as shown in Table II.

Table II

Transformer Rating—1,667-Kva Single-Phase 60-Cycle, 13.2 to 2.3 Kv. Copper Loss at 75 Degrees Centigrade—11,308 Watts. Iron Loss 3,850 Watts

	80% Load	100% Load	120% Load
High-voltage rise	42.1	56.7	68.5
Low-voltage rise	42.4	56.9	69.8
Ambient	27	31	29.5
Top-oil rise	36.5	49	60
Top of radiator, rise	34	45	56.5
Bottom of radiator, rise	22	34	43.5
Difference in radiator temperatures	12	11	13
Bottom-oil temperature	49°	65°	73°
Viscosity	0.070	0.045	0.036
Copper losses	7,100	11,750	17,500
Total losses	10,950	15,600	21,350
$\sqrt{\text{Losses}\times\text{Viscosity}}$	27.7	26.5	27.6

The following conclusion may be reached:

The difference between the top oil and average oil does not change greatly with load at constant ambient temperature, due to the nature of the oil flow and viscosity changes. If the velocity is sufficient to

cause turbulence, there is the possibility that some increase in temperature difference will occur.

Effect of Oil Temperature on Gradient Between Copper and Oil

Tests have been made on circular coils wound with 0.009 thickness of paper insulation at various oil temperatures, both in a vertical and horizontal position. In the case of the vertical coils, the oil temperature and copper temperature were measured at the top of the coil. In the case of the horizontal coils, the oil temperatures were measured both 2 inches from the coil horizontally, and also, in the duct between the coils and the tube inside the assembly. The oil temperature was found to be essentially the same in both places. The results of these tests are shown on Figures 3 and 4. It is of interest that the gradient for the vertical coils varies very closely to the 0.8 power of the watts per square inch for a given oil temperature, and that the gradient is higher for lower oil temperatures. It will be noted that low oil temperatures affect the gradient for horizontal coils very much, but at 80 degrees, the curves for both horizontal and vertical coils are substantially the same. This is very reasonable, as the viscosity of the oil at 80 degrees centigrade is very low, and permits nearly as easy flow into the horizontal ducts as in the vertical ducts.

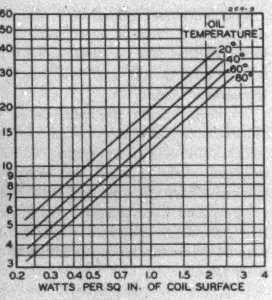


Figure 3. Gradients between oil and copper for model transformer coils—coils in vertical plane

There is one interesting fact that can be derived from Figure 4. If a transformer had an average of one watt per square inch dissipated on the coil surface at 60 degrees centigrade, the gradient would be 15 degrees. At 80 degrees centigrade, the watts per square inch would be increased to be approximately $(234.5+95)/(234.5+75)\times 1$, or 329.5/309.5 equals 1.07. With 80 degrees centigrade oil, the gradient would be about 12 degrees. A change in ambient temperature of 20 degrees centigrade, therefore, would make the temperature rise 3 degrees centigrade less. This effect is less pronounced for transformers with vertical coils.

Temperature Rises Under Transient Conditions

In order to determine how the temperatures varied during short-time overloads, a test was made at varying loads for different lengths of time on a 600-kva, three-phase transformer. The hottest spot temperatures were also measured by thermocouples in the second coil from the top.

Figure 5 shows the transient conditions between the oil and the copper, and since this is a fairly representative case, shows that this transient for most transformers will be completed in fifteen minutes, more or less, depending on the design and the current density in the copper. After this transient has ended, the determination of the hottest spot would be reduced to determining the difference between the average and top oil, and the gradient between the top oil and the hottest copper.

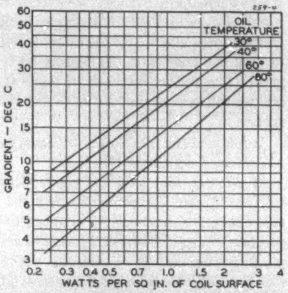


Figure 4. Gradients between oil and copper for model transformer coils—coils in horizontal plane

Since the top oil and the average oil have the same difference, or gradient, between them on overloads of long duration, it would follow that this might also be true on shorter-time overloads. To satisfy this condition, the difference between the average rise by resistance and the hottest copper rise should also be constant, since the gradient between the copper and oil, adjacent to it at any place in the column of coils, should be substantially a constant. This would not be true if the conductor insulation, ventilation, and so forth, were not the same for the top coils as for the body coils. Figure 6 shows the temperature gradients found between the top oil and both the hottest copper and the average copper. On the average, the difference between the hottest copper and the average copper was 8 degrees. The variation is within the limits of reasonable experimental error, and indicates that there would be no serious error in calculating hottest-spot temperatures using this assumption.

Table III

Time—Hours	Hot-Spot Temperature—Degrees Centigrade
1/4	140
1/2	135
1	130
2	125
4	120
8	115
24	110

Application of Calculated Hot-Spot Temperatures

The data obtained above are of interest because they can be applied to practical transformer design and operation. The most interesting application is in the determination of safe short-time overloads for transformers. In order to do this, a table of safe temperatures must be established. To illustrate the point, assume the temperature-time table, Table III.

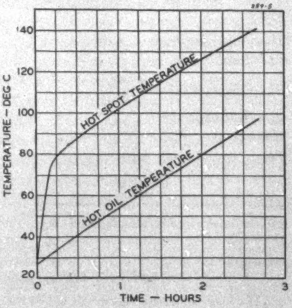


Figure 5. Transient temperatures in 600-kva transformer at 300 per cent load, starting cold

The values for hot-spot temperature and the times associated with them are a matter of judgment, and are based on tests of insulation under oil at various temperatures.[1] These values are very conservative, and higher values probably could be used when the present operating recommendations are revised. If we are to make a general application of these values, it is next necessary to establish average transformer characteristics. Such a study

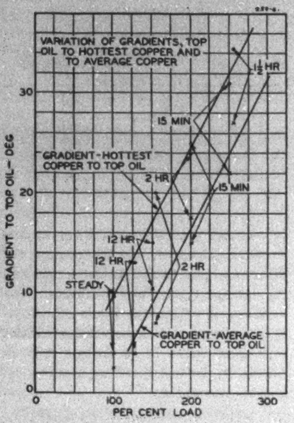


Figure 6. Variation of gradients, top oil to hottest copper, and to average copper

has led to the tabulation for power transformers shown in Table IV.

From other authors,[2] the approximate formula for transient oil rises has been derived as follows:

$$T_t = T_f(1 - {}^{-}\epsilon^{t/B}) \qquad (1)$$

where

T_f = Final temperature rise
t = Time, hours
B = Time constant $= \dfrac{T_f \times C}{L}$
C = Heat capacity
L = Losses

Table IV

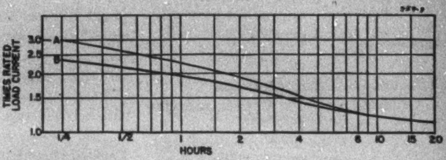
Ratio of losses, copper to iron, full-load	2:1
Time constant	4
Temperature rise—copper*	55 degrees
Hot-oil rise*	50 degrees

*Maximum allowed, not average values. Gradients of 5 degrees between copper and hot-oil rises are common.

The formula 1 can be shown to hold for any abrupt change in loading, so that if the transformer is fully loaded and at a steady state, a sudden increase in load will result in a transient determined by the difference between the final condition, under the overload, and the full load condition.

It appears that for many transformers the actual hottest-spot temperatures are in the order of 6 degrees or less above the average copper temperature. In this case, the actual hottest spot should not exceed 61 degrees. Also, the average oil rise will be 50−6 degrees or 44 degrees centigrade.

Figure 7. Emergency overloads for various times



Power Transformers
A—Following no load
B—Following full load

Assumptions at Rated Load
Ratio of losses = 2 to 1
Time constant = 4
Oil rise = 50°
Hot spot = 61°

Time in Hours	Times Rated Load Current Following: Full-Load	Times Rated Load Current Following: No-Load	Hot-Spot Temperature in 30° Ambient
¼	2.35	3.00	140
½	2.10	2.60	135
1	1.90	2.25	130
2	1.71	1.9	125
4	1.45	1.55	120
8	1.33	1.33	115
24	1.2	1.2	110

It is now possible to calculate the overloads which can be applied for the times and temperatures given. As an example, let us pick one hour, and 130 degrees centigrade. Cut and try methods are simplest, and we will assume the load to be 200 per cent.

At full load the total losses are equal to $3X$, where X equals the iron loss.

At 200 per cent load the copper loss will be $8X$, not correcting for temperature change. Correcting for copper temperature change, assumed to be 124 degrees centigrade, it would be $8X \times (234.5+124)/(234.5+85) = 9X$. The total losses would be $10X$. An ambient of 30 degrees centigrade is assumed.

Since the average oil rise is 44 degrees at 100 per cent load, at 200 per cent load, and with losses at 124 degrees centigrade, the ultimate rise would be $(10/3)^{0.8} \times 44$, or 115.0 degrees centigrade.

If the original rise was 44 degrees centigrade, the change in temperature rise is 115−44 or 71 degrees centigrade.

In one hour, the oil rise will be:

$$71(1-\epsilon^{-1/4}) = 71 \times 0.223 = 15.9 \text{ degrees centigrade}$$

The actual top-oil temperature will be $15.9+50+30=95.9$ degrees centigrade.

If no allowance for the reduced oil viscosity is made, the winding gradient is obtained by multiplying the gradient at 100 per cent load (=11 degrees centigrade) by the ratio $(9/2)^{0.8}$, which takes in account the increased losses, due to the change in copper temperature. There is an appreciable change in oil temperature, however, and Figure 4 and the previous discussion indicate that it should be safe to assume that the increase in resistance loss at higher temperatures is compensated for by the reduction in gradient, due to reduced oil viscosity. On this basis, the computed gradient for this case will be $(8/2)^{0.8} \times 11 = 33.3$ degrees centigrade.

The total rise is then 95.9 plus 33.3, equals 129.2 degrees centigrade, which indicates that 200 per cent load for one hour following full load will meet the temperature limits.

By using similar methods, all of the values can be calculated. Figure 7 shows such a table and curve, giving values of overload slightly below the computed values. Comparison of this with Figure 17, Proposed Recommended Practices of the American Standards Association, page 86, indicates the conservatism of the older table.

Table V. Table of Safe Emergency Loads for Different Times After Full Load

	Times Rated Load Current	
Time—Hours	Values From Figure 7	Values From ASA, Figure 17
½	2.10	1.6
1	1.90	1.38
2	1.71	1.25

Summary

A more accurate method than previously used for calculating temperature rises in transformers has been derived, and applied both to steady and transient conditions. This latter application, along with the recognition that newer transformers have both lower gradients and better oil protection, give the possibility of recommending much higher emergency loads than previously given to such apparatus.

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2. Transformer Engineering (book), L. F. Blume, C. Camilli, A. Boyajian, and V. M. Montsinger. Pages 300–03.

Electropneumatic Brakes for High-Speed Trains With Particular Reference to Their Electrical Features

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ELECTROPNEUMATIC brakes for main-line high-speed passenger trains are a recent innovation. Electropneumatic brakes have, however, been known and used for many years. The first steam road tests were made in 1887. The results were quite gratifying, but improvements in the pneumatic apparatus resulted in such satisfactory performance that the added expense and complication of electropneumatic control was clearly not warranted. Electropneumatic brakes have been used, however, in subway and elevated service, and in considerable quantity, for more than 30 years.

To understand why electropneumatic brakes have so generally been adopted for modern high-speed trains requires some comprehension of the limitations of a strictly pneumatic system and some grasp of the part played by the brake in modern transportation. The importance of the brake, as a factor in the expeditious movement of trains, is not generally appreciated. The brake system suffers, in a consideration of its influence upon the maintenance of high schedule speed, because its work seems entirely negative. That is, the purpose of the brake is to retard or stop trains, a result which can be attained, after a fashion, without any brake at all. Since the brake appears to be only assisting in an event which would take place anyway, its potentiality as a means to move trains faster over the road is not apparent at first glance. In fact, it seems anomalous to assert that a brake, which stops a train, has anything to do with covering ground, which obviously implies motion, but that this is actually the case will become apparent later.

Because the work done by the brake is of a negative character, it frequently happens that the technical difficulties involved in producing a satisfactory brake and the power embodied in it are not fully visualized by those who are not directly in contact with the braking situation. Many engineers are confronted with the problem of transferring heat energy into mechanical energy, as in steam or gas engines. Electrical engineers change, in motors, electrical energy into mechanical energy. Engineers, in general, are mainly preoccupied with obtaining mechanical energy from some other form of energy, whereas brake engineers, as a very special group, deal with the transformation of mechanical energy into heat. Although this transformation is the reverse of that ordinarily encountered, it is evident that many technical problems must be met with, a consideration of which is not pertinent to this paper.

It is believed, however, that some mention should be made of the power requirements of the modern brake. Neglecting friction, wind resistance, and so forth, it is obvious that, measured at the rail, the motor torque and the brake torque must be equal if a train is to be accelerated or decelerated at the same rate. But in actual practice, the brake torque is greater than the motor torque since the rate of deceleration exceeds the rate of acceleration. Furthermore, this higher brake torque is produced at the highest speeds. Consequently, since horsepower is proportional to the product of torque and velocity, the horsepower involved in deceleration is many times as great as the horsepower involved in acceleration. Thus, to decelerate a 1,000-ton train, at 100 miles per hour, at the rate of 2.2 miles per hour per second, involves the dissipation of 53,500 horsepower.

Horsepower may be used to indicate the rate at which a brake dissipates energy, since it is a less cumbersome expression than foot-pounds or Btu's per second and is also a concept familiar to engineers. But it should be noted that, from the heat viewpoint, the horsepower of a motor is a very different thing from the horsepower of a brake. The heating of the motor, caused by I^2R loss, friction, and so on, has a low value, expressed as horsepower, since it is but a small percentage of the output horsepower. The entire output of the brake, on the other hand, is heat. When expressing the energy dissipated by a brake in terms of horsepower, therefore, it must be remembered that the quantity of heat involved is many times the heat found in a motor of similar horsepower.

It is clear that in braking trains, mechanical energy is transformed into heat at a very rapid rate and that very quickly tremendous quantities of heat are generated. Fortunately, since a brake can be installed at each wheel, this heat does not have to be dissipated in some localized area, but instead can be dissipated at each wheel in the train. The problem of heat dissipation is thereby made much easier of solution. In addition, a brake at each wheel makes possible the shortest stop, because it enables the maximum retarding force to act upon the train. It is evident that since a brake has to be provided at each wheel, both in order to handle heat satisfactorily and to produce the necessary retardation, the air brake must provide for remote control of all these brakes from the head end.

Electrical engineers will recognize a certain similarity between a multiple-unit control system and an air-brake control system. In the electrical system, each car is fitted with a control group or box; in the pneumatic system, each car is fitted with a triple valve or its equivalent of universal or control valve. In the electrical system, the controller is moved from its off to its on position by remote control from the head end; in the pneumatic system, the triple valve is moved from its release to its applied position, again by remote control from the head end. In the electrical system, the controller progresses or notches up under remote control from the head end; in the pneumatic system, the triple valve graduates on or applies the brake fully, also

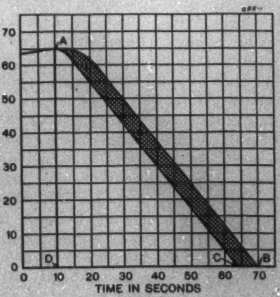

Figure 1

Paper 42-55, recommended by the AIEE committee on land transportation for presentation at the AIEE winter convention, New York, N. Y., January 26-30, 1942. Manuscript submitted November 12, 1941; made available for printing December 19, 1941.

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under remote control from the head end.

If there are certain similarities between a multiple-unit control system and an air-brake control system, there are three striking dissimilarities. The first of these is that the electrical system employs several control wires, whereas the pneumatic system employs but a single control pipe, the familiar brake pipe. The second dissimilarity is that the control wires of the electrical system transmit control current only and do not carry the main motor current; the single pipe of the pneumatic system handles not only air for control purposes, but also the air for charging the brake cylinders, which involves substantial volumes of air. The third dissimilarity is that current flows through the control wires with the speed of light, whereas air flows through the brake pipe with the velocity of sound as the limit.

It is well-known that car brakes are caused to be applied by reducing the pressure in the brake pipe, but this operation should be examined in some detail in order that the reasons for electropneumatic control may be better understood. It is a comparatively simple matter to move a car controller from its off to its on position by remote control from the head end, but a somewhat more difficult operation to move a control valve from its release to its applied position, the equivalent of from off to on with the controller. This difficulty arises because the brake pipe is used, not only as the control medium, but also as the means of transporting large quantities of air to the car supply reservoirs, from which the brake cylinders are charged. Because of this function, which means that the feed grooves are open, the control valves, in release position, are not in a condition to respond to slight reductions in brake-pipe pressure occurring at a relatively slow rate.

To move the control valves from their off to their on position, it is necessary, therefore, to make a slight reduction in brake-pipe pressure throughout the train rather rapidly. This reduction cannot be made too rapidly, however, because then an emergency and not a service application would develop, not to mention certain other undesirable results. In order that this reduction in pressure may occur quickly throughout the train and with minimum pressure gradient from head to rear, control valves are provided with a quick-service feature which causes the pressure to be vented locally, at each control valve, at the same time that a reduction is being made at the brake valve. With this arrangement, the control valve on the last car of a long train can be moved from its off to its on position in a few seconds. It has been found that the time is roughly proportional to the linear length of the brake pipe; that is, in long trains about one second is required for each 300 feet of brake pipe. In other words, if a train were of such length that the control valve on the last car was 1,000 feet from the brake valve, this control valve would move from its off to its on position about 3.3 seconds after the brake valve had been placed in service position.

It should be noted that 300 feet per second applies to the last car only. The combined action of the quick-service feature and the flow of air from the rear is such that the first car does not apply until shortly before the last car. In consequence, at the expiration of a brief interval proportional to the length of the train, control valves, from head to rear, move from their off to their on positions with a fair approach to simultaneity.

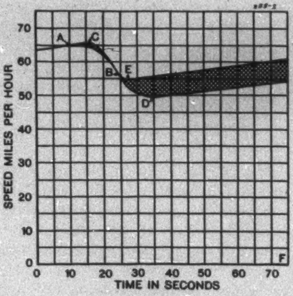

Figure 2

The operation just described covers the movement of the control valves throughout the train from release to application position, in which position the brake cylinders are charged with compressed air from the car reservoirs. In order that the brake cylinders may be charged to full-service pressure, the brake-pipe pressure must be reduced, on each car, 24 pounds or, say, from 110 to 86 pounds. It is clear that the pressure in a long pipe, commencing at the head end, cannot be lowered 24 pounds throughout its length, except when an appreciable time is available, and particularly when the gradient in pressure, from head to rear, must be held to a minimum. With the most modern pneumatic equipment, including continuous quick-service valves, it takes roughly 15 seconds, after placing the brake valve in service position, to build up full-service cylinder pressure on the last car of an 18-car train.

The release of the brakes, or the movement of the car control valves back to off position, is accomplished by building up pressure in the brake pipe. After a full service application, as stated above, the brake-pipe pressure throughout the train, is roughly 86 pounds, and this pressure must be restored to about 110 pounds before all pressure can be completely released from the brake cylinders. Since the brake-pipe pressure is restored from the head end and against the drain of car reservoirs which are, at this time, recharging themselves from the brake pipe, a substantial time element is involved, although this time is still a matter of seconds and not greatly different from the time to apply all brakes fully.

It is clear that the operations just described involve transient pneumatic phenomena. Electrical engineers appreciate the difficulty attached to understanding and controlling transient electrical phenomena. They probably will not dissent from the statement that transient phenomena in a gas, or elastic medium, present technical problems at least as difficult. Many of these problems can be circumvented by utilizing electrical control means.

Since 1887 at least, it has been understood that the time element involved in applying or releasing brakes could be greatly reduced by the use of electricity and since that time many systems of electropneumatic control have been devised, and several types have been placed in actual service. The type used on the modern high-speed train may be called a two-pipe system. That is, a second pipe, termed the straight air pipe, is employed as well as the familiar brake pipe. The straight air pipe only is used during electropneumatic operation.

Each car is equipped with electropneumatic valves. When these valves are energized from the head end, pressure is built up locally on each car, in the straight air pipe, so that the pressure in the straight air pipe throughout the train can be brought up quickly to full service value. Suitable pneumatic relays, controlled by straight air-pipe pressure, charge the car-brake cylinders to the pressure existing in the straight air pipe and in the same time. With this system of control, full-service cylinder pressure can be secured in about four seconds from brake-valve movement on all cars of a train of any length. To release the cylinder pressure, the electropneumatic valves are de-energized, which vents the straight air-pipe pressure locally throughout the train. Brake-cylinder pressure is consequently rapidly released.

With this system, it is evident that in service operation, three results, which cannot be obtained with pneumatic control alone, are accomplished.

1. Pressure commences to appear in all brake cylinders throughout a train of any length at substantially the instant the brake valve is placed in service position.
2. The cylinders throughout the train can be charged simultaneously to full pressure in a time fixed by the designer's option.
3. Pressure in the cylinders throughout the train can be completely exhausted simultaneously in a time again fixed by the designer's option.

It will be noted that the time saved by electropneumatic control is a matter of seconds. The question arises as to why a few seconds is an item of any great moment in the operation of high-speed main-line trains, which make few stops and few slowdowns. If one of these trains, however, traveling at 100 miles per hour or about 150 feet per second, has to make an emergency stop to save lives and avoid property damage, the importance of saving a few seconds in brake-application time is self-evident. This aspect of electropneumatic control is so well understood that no particular comment seems called for. But since the effect of electropneumatic control on service operation is less widely understood, some comment in this connection may be of value.

Figure 1 illustrates a situation which arises more or less frequently in actual service. A train is moving along the track, still accelerating, when it encounters, at *A*, an unfavorable signal aspect and under circumstances which indicate that a service-application stop must be made at once. Power is immediately shut off and the brake valve placed in service position. With pneumatic brakes, the train decelerates along the line *AB;* with electropneumatic brakes, along the line *AC*. The stop distance with the pneumatic train is represented by the area *ABD;* with the electropneumatic train, by the area *ACD*. The cross-hatched area is the saving in stop distance, which, obviously, is far from negligible.

This chart has been prepared to show the gain brought about by a single factor, that is, the use of electricity, and does not refer to any specific train. It assumes that a full service application, with either brake, produces a retardation of 1.25 miles per hour per second, and that this retardation is constant, irrespective of speed or temperature. When the brake is partially applied, the retardation is taken as directly proportional to the cylinder pressure. The cylinder-pressure build-up is assumed to duplicate laboratory tests of an 18-car train. The retardation is supposed to depend entirely upon the car-cylinder pressures, that is, the locomotive brake blends harmoniously with either system of car brakes. Internal and wind resistance is neglected.

None of these assumptions corresponds precisely with the conditions found in actual operation. Notwithstanding, it is believed that the chart fairly indicates the order of the shortening of service-stop distances, on a comparative basis, accomplished by adding electricity to the pneumatic-braking system. The saving reduces as the number of cars in the train decreases but never entirely vanishes, because the pneumatic-application time is always greater than the electropneumatic time, even for a one-car train.

Since signal spacing is based upon service-stopping distances, the situation described above is of considerable practical importance. Another aspect of service-stopping distances was mentioned by R. P. Johnson, chief engineer, Baldwin Locomotive Works (*Transactions* of the American Society of Mechanical Engineers, October 1941, page 614) before the society, namely:

> "Modern high-speed operation presents problems in deceleration as well as acceleration, but the paper does not say much about this. On some railroads, such as the New Haven, station stops may be rather close. If, on such a road, the decelerating force were relatively low, the train could not, within the distance, be permitted to attain the maximum speed of which it is capable."

Figure 2, which is based on the same assumptions as Figure 1, illustrates a situation frequently encountered in regular service. The train is proceeding along the track, still accelerating. Suddenly a condition arises which may call for a brake application. This condition may be an unfavorable signal indication which is not clearing as expected, an automobile crossing the track, a track-repair gang somewhat slow to move, or any unexpected interference, of a temporary nature, which may call for a stop. A brake application is deferred as long as possible but with pneumatic brakes, in order to stop short of the interference, the brake valve has to be placed in service position at *A* and the train decelerates along the line *AB*. To stop at the same point with faster-acting electropneumatic brakes, the brake valve does not have to be placed in service position until several seconds later or at *C*. The train then decelerates along line *CB*.

At *C*, the temporary interference clears; that is, the signal aspect becomes favorable, the automobile completes its passage across the track, the track gang gets out of the way, and so on. The brake valve is, therefore, immediately placed in a position to release the brakes. The train continues to decelerate, of course, until the brakes are completely released, which occurs at *D* with the pneumatic brakes and at *E* with the electropneumatic. Power is then applied and the train accelerates. The cross-hatched area indicates the greater distance covered by the electropneumatic train in the time *OF*. It will be noted also that at *F*, the electropneumatic train is at a higher speed than the pneumatic train and is consequently still gaining distance.

It has been stated that in Figures 1 and 2, it is assumed that a full-service application of either the pneumatic or the electropneumatic brake produces the same retardation. The savings discussed above

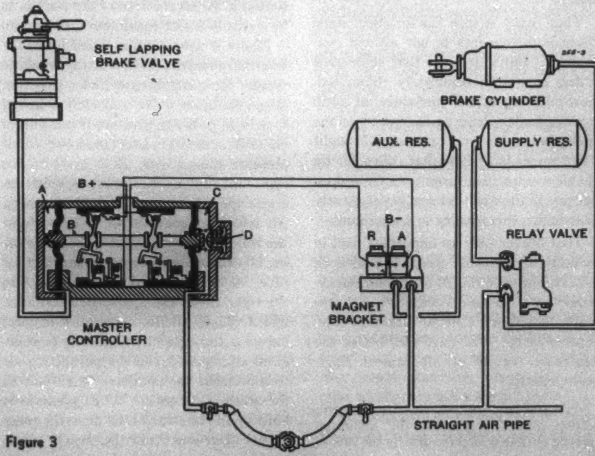

Figure 3

are, therefore, those to be credited entirely to the use of electricity. It will be observed that these savings are obtained in situations where the brake valve has to be placed in service or release position without any prior warning. At regular station stops or slow-order curves, the need for a brake application or release can be anticipated, in which event the savings under discussion, with skillful handling, largely disappear. But there are so many occasions in actual operation, apart from emergency stops, when the brake must be applied or released without advance notice that electropneumatic brakes contribute materially to the expeditious movement of high-speed trains.

In this connection, the release function is of particular interest. Since a long time and a considerable distance are needed to accelerate a train to the high-speed zone, it is important that the high speed, once attained, be held as long as possible; that is, that slowdowns and stops be reduced to the absolute minimum. The urgent necessity of such a course can be readily appreciated by anyone who has noted the effect of one or two stops, or a long detour, on the mileage covered in an all-day automobile trip. The railroads, from the beginning of high-speed operation, have fully appreciated this necessity. Large sums of money have been spent to eliminate speed restrictions at curves, track intersections, grade crossings, and so on. Meets have been arranged so that high-speed trains would not be called upon to reduce speed. Steps have even been taken with track-repair gangs so that their presence would not interfere with the maintenance of high speed.

That brake release has an effect upon these improvements is not always considered. But it is clear that since slow orders cannot be completely eliminated, slow-releasing brakes introduce an additional penalty, because the speed of the train, due to slow-brake release, is brought down somewhat below that called for by the slow order. Electropneumatic brakes, because of their fast release, assist greatly in reducing this penalty to a minimum.

That brakes play an important part in maintaining schedule speeds is evidenced by a statement by A. A. Raymond, superintendent of fuel and locomotive performance, New York Central (*Railway Age*, September 20, 1941, page 446) before the American Society of Mechanical Engineers, namely:

> "While acceleration only has been shown, getting over the road in the minimum amount of time is affected also by the rate of deceleration, although, in general, more time is lost accelerating than decelerating."

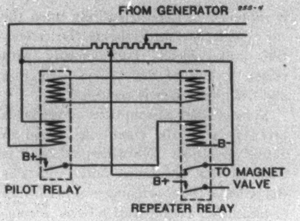

Figure 4

A detailed description of the operation of the complete electropneumatic brake does not come within the scope of this paper. In fact, it appears that such a description would possess only a very limited appeal. It is thought, however, that certain features of the brake involving problems of electrical control, which are not widely, nor ordinarily encountered, will interest electrical engineers in general.

It has been emphasized that electropneumatic brakes can be quickly brought into action, or as quickly taken out of action. In addition, it has been previously pointed out in this paper that a train-braking system comprises a large number of individual brakes, or one per wheel, and that they must all be controlled remotely from the leading unit. The retarding force at each wheel is created by brake-shoe friction; the brake-shoe friction is proportional to the brake-shoe pressure; and the brake-shoe pressure is proportional to the brake-cylinder pressure. It follows that regulation of the cylinder pressure within close limits is essential if the retardation of the train is to be controlled with maximum satisfaction.

Figure 3 shows, in simplified form, the essential elements of the electropneumatic system for controlling cylinder pressure. When the brake valve at the left is moved to service position, pressure is built up in chamber A of the master controller. This pressure causes shaft B to move to the right and close a set of contacts which energize the release wire from the battery. All release-magnet valves R throughout the train are then energized, thereby closing all exhaust ports from the straight air pipe. Continued movement of shaft B to the right closes the application contacts, which causes all the application-magnet valves A throughout the train to be energized and opened, thereby permitting air to flow from the auxiliary reservoirs to the straight air pipe. When pressure is built up in the straight air pipe, the relay valves open and from the supply reservoirs, charge the brake cylinders to the pressure existing in the straight air pipe. Meantime, pressure from the straight air pipe has been building up in chamber C of the master controller. When this pressure almost equals that in chamber A, spring D moves shaft B to the left and opens the application contacts. The application magnets close and the cylinder pressure remains equal to that in chamber A.

To release the cylinder pressure, the brake valve is moved to release position which releases the pressure in chamber A. Shaft B then moves to the left and opens the release contacts. All release-magnet valves are then de-energized and straight air-pipe pressure is exhausted to atmosphere at each magnet bracket. Exhausting the straight air-pipe pressure causes the relay valves to move to a position in which the brake cylinders are connected to atmosphere. The brakes are thereby released.

The very simplicity of this control system tends to obscure the many advantages it possesses. These advantages represent such an advance in the brake art that it is believed attention should be directed to them.

The brake valve employed with this system retains an old name but actually the mode of operation is entirely changed. The self-lapping brake valve is essentially a two-way regulating valve; that is, it regulates the pressure admitted to or discharged from chamber A. When the brake-valve handle is moved to the right, out of its normal or release position, each position of the handle calls for a definite pressure. In other words, when the handle is placed in a certain position, the pressure in chamber A is increased or decreased, depending upon whether it is less or greater than the pressure called for by the position of the brake-valve handle. The pressure can be increased or decreased at will in very small steps. It is independent of the volume of chamber A or the pipe attached thereto and is not affected by any reasonable leakage from volume or pipe. Those who are acquainted with old-style brake valves will appreciate what an advance in the art this self-lapping brake valve represents.

For practical purposes, the master controller moves to its application position, the application-magnet valves are energized and the relay valves are opened coincident with placing the brake valve in an application position. It has been previously explained that several seconds are required to move the control valves of the purely pneumatic system from their off to their on position with trains of customary length. All this time is eliminated by electropneumatic control.

Furthermore, the time of charging the brake cylinders to full pressure is not contingent upon dropping the pressure throughout a long brake pipe. Instead, the application-magnet valves on each car can charge the small straight air-pipe volume of each car very quickly and the brake-cylinder pressure, through the action of the relay valves, keeps pace therewith. Each application-magnet valve is provided with a choke so that the time of fully charging the straight air pipe is prefixed at about four seconds. It is not necessary to change the size of this choke to compensate for variations in car length, and so on, because the straight air pipe itself averages out variations since it is continuous throughout the train. If a magnet valve should become inoperative, this continuous pipe supplies the necessary air from front and rear. If, during an application, the pressure in the straight air pipe should reduce through leakage, which is probable since its volume is small, the master controller promptly moves to the right, re-energizes the application-magnet valves, and restores the loss in pressure.

Electropneumatic brakes can be applied to one-half their full-service effectiveness in about two seconds. It is clear that if the pressure build-up had to be shut off by moving a brake valve back from service to lap, very considerable skill would be required, if indeed it could be done with reasonable accuracy. With the system shown in Figure 3, the brake valve is moved half way toward full-service position, which produces one-half the full-service pressure in chamber *A*. As soon as this pressure is attained in the straight air pipe, and hence the brake cylinder, the master controller de-energizes the application-magnet valves. It is plain that this system makes it possible to apply and release brakes partially, despite the very short times involved, easily, positively, and uniformly.

It is apparent that with this system, the control of cylinder pressure is not influenced by the number of cars that may be in the train. This is in decided contrast to a purely pneumatic system in which the time to obtain a certain cylinder pressure changes with the number of cars in the train. That brake action should be independent of train length is obviously so desirable as to call for no extended comment.

The action of the relay valve, which directly charges the brake cylinder, is not changed by variable brake-cylinder volume. Variations in brake-cylinder pressure caused by variations in piston travel, a troublesome problem with older pneumatic brakes, are entirely overcome. Moreover, the relay valve automatically maintains the brake-cylinder pressure against leakage.

The net result of this control system is that any cylinder pressure desired, requiring either an increase or a decrease over the pressure previously existing, may be easily, positively, and uniformly obtained in a very short time which is always the same irrespective of train length; and independent of variations in pipe volumes, changes in brake cylinder piston travel, or brake cylinder leakage. Similar results, particularly with respect to time, cannot be attained with a one-pipe pneumatic system.

It is well known that the friction of cast-iron brake shoes, for a given pressure, decreases as the speed increases. If the same retardation is to be obtained in the high-speed brackets as in the low, a greater brake-shoe pressure must be employed at the higher speeds. It has been found empirically that satisfactory results are obtained if 100 per cent of this higher pressure is employed at speeds above 65 miles per hour, 80 per cent between 65 and 40 miles per hour, 60 per cent between 40 and 20 miles per hour, and 40 per cent at speeds below 20 miles per hour. To make these changes automatically, it is evident that a speed-recognizing device is required. For this purpose, a generator, usually axle-mounted, is employed.

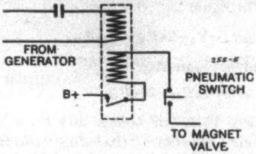

Figure 5

A generator to measure speed is a familiar arrangement but such generators ordinarily provide energy only sufficient to operate indicating instruments. The generator here considered has to supply enough energy to operate a system of relays, which means that at 100 miles per hour the generator output is about six watts. Since the relays operate on direct current, a d-c generator is used, and operation in either direction is taken care of by a directional relay.

One problem with relays, of course, is that they pull in with one voltage and drop out with an appreciably lower voltage. This characteristic has to be overcome with the speed-control system, since it is desired that speed recognition be as sharp and definite as possible. The method used is illustrated by Figure 4.

When the generator has built up sufficient ampere turns in the lower coil of the pilot relay to pull in the armature, the closing of the contacts energizes the lower coil of the repeater relay. The flux thereby built up induces a current in the upper circuit which adds to the ampere turns of the pilot relay, so that its armature is pulled in without hesitation or fluttering. Opening of the upper contacts of the repeater relay cuts additional resistance into the circuit of the lower coil of the pilot valve. It consequently drops out on a higher voltage than would otherwise be the case.

The result achieved with this combination is that if the pilot relay pulls in at a speed of 69 miles per hour, it drops out at no less than 65 miles per hour and ordinarily at a somewhat higher speed. This four-mile-per-hour maximum range represents a drop of 5.8 per cent. Despite the fact that this device must be sufficiently rugged to withstand the shocks of railway service, its range approaches that of a household thermostat.

This generator can also be used to prevent wheel sliding. When used for this purpose, the generators are termed "Decelostats" and one is required per axle.

It is obvious that not very much time is available to prevent wheel sliding. When the wheel is rolling freely with brakes applied, a very small portion of the braking torque is devoted to destroying the angular velocity of the wheel and axle assembly, but the greater portion is balanced by the counter torque set up at the rail. But the retarding force set up at the rail cannot exceed the product of the coefficient of adhesion and the pressure actually exerted by the wheel on the rail. If the retarding force is near the adhesion limit, therefore, it is clear that the wheel will slip if

1. The braking torque, for any reason, increases abnormally.
2. The pressure of the wheel on the rail is reduced because of oscillations, and so on.
3. The coefficient of adhesion decreases because a section of bad rail is encountered.

If one or more of these conditions arise, the wheel and axle assembly loses angular velocity with great rapidity because the braking torque is directed almost entirely to arresting the rotary motion of the wheel and axle assembly. The wheels may cease revolving completely in a time which may not exceed 1 second.

A mechanism to prevent wheel sliding, therefore, must recognize wheel slippage

A Control System for Modern Multiple-Unit Rapid-Transit Cars

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ASSOCIATE AIEE

MUCH has been accomplished in recent years in the development of refined systems of control for traction motors as applied to buses, trolley coaches and street cars. Chief among the innovations is the extensive use of some form of electric braking—either as a holding brake for limiting speed when descending a grade or as a service brake for retarding the speed of the vehicle each time a slowdown or stop is made.

The latter type of brake is used as the principal means of braking PCC (Presidents' Conference Committee) cars, being supplemented by magnetic track brakes to obtain rates of retardation beyond the adhesive limit of wheels to rail and complemented by some form of friction brake to provide means of retarding the car at speeds at which the electric brake is not effective and to hold the car at standstill on a grade. The electric service brake is obtained by connecting the motors so that they operate as series-excited generators, the output being absorbed by the same resistors used for acceleration. To control this output under widely varying and continually changing conditions of speed and load requires the use of a novel switching and regulating scheme.

The success with which the equipment for PCC cars has met in street railway service has led to its adaptation to multiple-unit service. A notable example of such adaptation is the "compartment car", a number of which have been placed in operation by the New York City Transit System (Brooklyn-Manhattan Transit division). Because of conditions peculiar to operation in multiple-unit and from third-rail supply in subway and elevated service, various modifications and additional features (as compared to PCC car equipment) are provided.

Characteristics of Dynamic Braking

Methods of determining the accelerating and braking characteristics of d-c series-excited machines and control are well known. For the elementary circuits illustrated by Figure 1 the following relations apply:

In Figure 1a,

$$2I_MR_A=V_L-2nE_M-2I_MR_M \quad (1)$$

and, in Figure 1b,

$$2I_MR_B=2nE_M-2I_MR_M \quad (2)$$

where V_L is line voltage and R_M is the motor resistance (including effective brush-contact resistance).

Equation 2 is of particular interest, because it illustrates that, since E_M is a function of I_M, the braking resistance is a linear function of speed for any constant value of current I_M. In fact, from equation 2

$$\frac{n}{R_C}=\frac{I_M}{E_M} \quad (3)$$

where $R_C=R_B+R_M$=total circuit resistance.

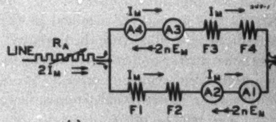
If, therefore, the ratio I_M/E_M for each value of field strength used in braking be plotted as a function of I_M as shown in Figure 2, very useful curves for determining braking characteristics of the machine are provided. The curves are the basic form of all constant-resistance braking curves of the machine as, by multiplying the ordinate scale by any value of R_C, the curves become the current-speed characteristics for that circuit resistance. For example, with a circuit resistance of three ohms, the GB-1198 motor (full-field) will generate 200 amperes at 3,000 rpm. By further multiplying the ordinate scale by the proper factor determined by gear ratio and wheel diameter, the speed can be expressed in the more familiar unit of miles per hour car speed.

The curves also clearly show, for each field strength, the critical ratio of speed to circuit resistance below which the machines will not generate appreciable current. At this critical ratio the machines are very sensitive to slight changes in speed and resistance, this condition representing operation in the unsaturated portion of the magnetization curve.

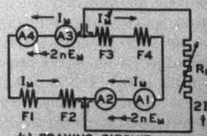
Switching and Spotting

Obviously, in the interests of safety and efficient operation, the response of dynamic braking to the control of the operator or to automatic application for an emergency stop must be prompt but free of surges which might cause discomfort or injury to passengers.

Switching from accelerating to braking connections is facilitated by the use of motors connected permanently in parallel relation as it is necessary only to transfer the location of the resistors in the circuit.



(a) ACCELERATING CIRCUIT



(b) BRAKING CIRCUIT

Figure 1. Elementary motor-circuit connections showing relations of current and electromotive force

I_M—Motor current
E_M—Motor electromotive force per unit speed
n—Speed
R_A—Variable accelerating resistance
R_B—Variable braking resistance

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promptly and must act quickly to release brake cylinder pressure. How this can be accomplished with a generator is illustrated by Figure 5. The generator charges a condenser through a unidirectional relay. If the wheel is losing angular velocity at an abnormal rate, as it does when the wheel slips, the generator voltage drops at an abnormal rate. The condenser then discharges a current sufficient to trip the sensitive relay, which causes a magnet valve to be energized and pressure to be released very rapidly from the brake cylinder.

In conclusion, it should be pointed out that electropneumatic brakes involve control of the brake-cylinder pressure from the brake valve and do not necessarily include speed governor or "Decelostat" control. That is, speed governors and "Decelostats" may or may not be used with electropneumatic brakes.

It is not necessary to operate the motor reversers and, in the braking connection, the exciting fields are "cross-connected" to insure equal division of load between the two parallel groups of machines.

It is not enough to merely establish the braking connections, but the circuit resistance must also be quickly adjusted to the value which will give the desired value of braking current at any speed at which the brake may be applied. This operation has been termed "spotting" the control, and various schemes have been used to accomplish the desired results.

One scheme which has been successfully used is to arrange that, regardless of car and motor speed, the braking resistance be at (or near) its maximum value at the time of switching to the braking connections, and then to rapidly reduce the resistance until the current builds up to the desired value. This requires, regardless of the means for controlling the resistance, that the control be capable of rapid operation and that its current-responsive regulating system be capable of quickly stopping operation before the resistance has been reduced to too low a value. The principal advantage of this scheme is that the braking sequence is practically independent of the accelerating sequence, and a "switching step" of acceleration control can be provided wherein the accelerating resistance can be retained at maximum value.

The principal factor governing the correct value of braking resistance is, however, the speed of the car and motors. This suggests, if the acceleration sequence be suitably arranged and controlled in relation to motor speed, that braking current can be obtained immediately upon switching to the braking connection without requiring that the resistance control operate to change the load resistance. Such ideal operation cannot be attained in practice because of the wide ranges of accelerating and braking currents used, but is approximated in the control systems now generally used for PCC cars. Such systems permit the use of a less powerful drive for the resistance controller but requires, even on the "switching step", that the acceleration sequence progress so as to maintain the required relation of resistance to car speed.

Coasting Operation

A refinement of the spotting operation can be provided by utilizing the interval in which the car may be allowed to coast before the application of braking. This is done by automatically switching the motor circuit to the braking connection and allowing the braking sequence to progress immediately after the operator shuts off accelerating power. However, under this condition, the braking current is held to a very low value and the exciting fields are partially shunted to maintain the resulting retardation at a negligible value. The control thereupon operates to establish the circuit resistance at the critical value corresponding to that particular field strength and car speed.

By proper choice of field strength and value of current used for coasting, the equipment may be made to provide immediate response to the application of dynamic braking simply by the operation of unshunting the exciting fields. As an example, from Figure 2, if the coasting current is maintained at 20 amperes with fields partially shunted, a current of approximately 95 amperes will be obtained by full-field operation without change in either motor speed or circuit resistance. Thereafter this or higher values of braking current are obtained by the resistanee control operating automatically in response to a current-sensitive relay to maintain the speed-resistance ratio at the value corresponding to the desired braking current.

Specific Connections and Operating Sequence

Figure 3 illustrates, with some modifications described later as being incident to multiple-unit operation and a particular type of service, a complete motor circuit for a PCC car. The resistor section *R*1-*R*2 indicated as having a tap which can be moved from position *A* to position *B* takes the form of a rheostatic, or "commutator", controller as shown by Figure 4. This device is essentially a stationary commutator with 137 active bars on about 330 degrees of the periphery, each connected by copper straps to resistor elements. Each active commutator bar, therefore, represents a tap on the whole resistor section *R*1-*R*2. Six carbon-tipped fingers or "brushes" are carried in constant contact with the surface of the commutator by an arm driven by a small reversible motor so as to rotate in either direction over the active bars. Mechanical stops are provided to prevent rotating the brushes over the inactive commutator bars which, with their slot insulation, serve to insulate resistor terminals *R*1 and *R*2 from each other over the remaining 30 degrees of periphery.

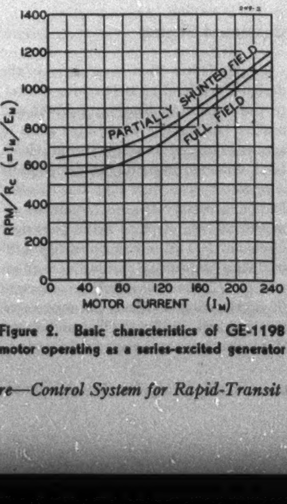

Figure 2. Basic characteristics of GE-1198 motor operating as a series-excited generator

The direction of rotation of the pilot motor and brush arm is controlled solely by the combinations of contactors which may be closed, and the speed is controlled primarily by the operation of accelerating and braking relay *ABR*.

For an acceleration from standstill to maximum speed, line contactors *LB*1, *LB*2 and *LB*3 close to complete the power circuit with all of rheostatic controller resistance *R*1-*R*2 and resistor sections *R*2-*R*3 and *R*3-*R*4 in series with the motors. The initial value of current and tractive effort so obtained is equivalent to a rate of only about 0.4 mile per hour per second for the usual weight of car.

Contactors *C*1 and *C*2 close in sequence to leave only the rheostatic controller in series with the motors and thus, in two steps, increase the current and tractive effort to approximately the value required for acceleration at the minimum service rate. The rheostatic controller then operates to move its brush arm from position *A* towards position *B* under control of relay *ABR* which, in turn, is responsive to current in its operating coil *ABR* (*a*). If a high rate of acceleration is required, relay *ABR* is made responsive to a corresponding value of current and the rheostatic-controller brush arm is permitted to move rapidly until this current is obtained. Thereafter, the speed of brush-arm movement is closely regulated to maintain constant current through the motors as the car speed increases.

When the brush arm reaches position *B*, the motors are operating at full voltage and contactor *LB*4 closes to prevent the re-insertion of resistor *R*1-*R*2 when the brush arm subsequently moves back towards position *A*. At this point in the sequence, transfer switch *TS* operates to open its contact (*a*) and close contact (*b*). This has no immediate effect in the motor circuit but is in anticipation of a following coasting or braking sequence. As the brush arm returns to position *A*, field-shunting contactors *FS*1, *FS*2, *FS*4 and *FS*3 are closed in sequence by circuits

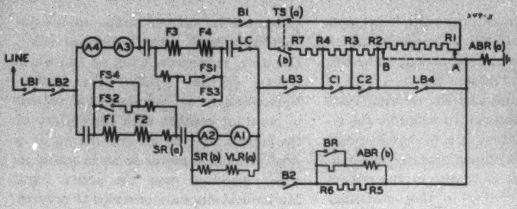

Figure 3. Motor-circuit connections for accelerating and braking

Figure 4. Rheostatic motor controller

controlled by cams mounted on the brush-arm shaft of the rheostatic controller.

The motor circuit may be switched to the braking connections at any time in the accelerating sequence by opening all contactors which may have closed and immediately closing braking contactors *B*1 and *B*2. As transfer switch *TS* and the rheostatic-controller brush arm do not change position during this operation, the value of braking resistance initially obtained in the braking circuit is determined by the status of the accelerating sequence just preceding the switching operation. Thus, if the acceleration sequence had been completed with rheostatic-controller brush arm returned to position *A*, the initial braking resistance would be at a maximum value, or the sum of all resistances shown. On the other hand, if the circuit were switched to braking connections at some low speed such that the brush arm was still moving from position *A* toward position *B*, the initial braking resistance would be only that part of section *R*1-*R*2 which had been cut out during the partial acceleration, plus the resistance of section *R*5-*R*6. This latter section is never cut out of the braking circuit, because it has been found impractical—except by means otherwise detrimental—to automatically regulate the braking current at very low speeds where the response of the motor to changes in speed-resistance ratio is very sluggish.

While either coasting or braking, the speed of brush-arm movement is again controlled primarily by the operation of relay *ABR* which is now responsive to an operating coil *ABR* (*b*) connected so as to be energized by the voltage drop across resistor section *R*5-*R*6. By use of a suitable control resistance in series with this operating coil, the range of operation of the relay in respect to braking current through resistor *R*5-*R*6 can be readily changed from a very low value, corresponding to coasting current, to high values, corresponding to normal service and emergency braking, by allowing contact *BR* to open.

The complete coasting and braking sequence from high speed consists of first moving the rheostatic controller brush arm from position *A* towards position *B*, transfer switch contact *TS* (*b*) having been closed and contact *TS* (*a*) having been opened in the preceding acceleration sequence. When the brush arm reaches position *B*, the transfer switch operates to open contact *TS* (*b*) and close contact *TS* (*a*). Since the resistance of sections *R*2-*R*3-*R*4-*R*7 is essentially the same as the resistance of the rheostatic controller resistor, the operation of the transfer switch re-inserts the resistor *R*1-*R*2 in the braking circuit without surge due to abrupt change of load resistance. As the car speed continues to decrease, the rheostatic-controller brush arm moves back to position *A* until, finally, only resistor *R*5-*R*6 is left in the circuit and the dynamic braking "fades out" at very low car speeds.

As previously indicated, the braking connections may be established at any speed and, therefore, the braking sequence described may be foreshortened by any amount.

The relation of braking-sequence progression to speed of the car also permits the switching to accelerating connections and the reapplication of power quickly and smoothly at any speed. A typical diagram showing the relation of sequence progression to car speed for extreme rates of acceleration and braking is shown in Figure 5.

Cushioned Shutoff

Emphasis has often been given, in the design of accelerating and braking control, to the smooth application of power and brakes, particularly where high rates of acceleration and braking are provided. It is quite as important that if, while accelerating at a high rate, the operator finds it necessary to coast, power be removed in easy steps to avoid passenger discomfort. This is accomplished by introducing sequential opening of contactors *C*2, *C*1, the field-shunting contactors (if accelerating sequence has progressed that far) and, finally, the line contactors. If it is necessary to make a brake application, however, the sequential operation is automatically nullified and the switching of the motor circuit to braking connections is obtained without delay.

A cushioned release of high rates of dynamic braking is obtained by the normal operation of partially shunting the motor fields when coasting. The braking current does not immediately drop to the normal coasting value because of the relative characteristics of braking with full and partially shunted fields.

Control of Rheostatic Motor Controller

Figure 6 shows the complete circuit for speed and directional control of the pilot motor which drives the rheostatic controller brush arm. The motor is provided with separate series-exciting fields for each direction of rotation so as to simplify the reversing operation by contactor interlocks. It should be noted that the direction of rotation is always such as to reduce traction-motor circuit resistance, whether accelerating or braking. No voltage, therefore, appears between the commutator bars of the rheostatic controller and the trailing edges of the carbon-tipped fingers, and the voltage at the leading edge is so low as to preclude any possibility of burning or sparking.

Control of the speed of the pilot motor in response to accelerating or coasting and braking currents is provided through the medium of relay *ABR* which, at a predetermined current determined by tension of a restraining spring, picks up to short-circuit the pilot-motor armature. Besides the operating coils (*a*) used during acceleration, and (*b*) used during coasting and braking, a third coil (*c*) is in series with the circuit controlled by *ABR*. This coil provides a powerful anticipatory response to increasing current in the traction-motor circuit to prevent "overshooting" and, at the regulated value of traction-motor current, causes the relay to operate on one or more of the sequentially closed contacts with a vibrating action such that the armature voltage and field excitation of the pilot motor and, therefore, the speed of brush-arm movement is regulated to maintain constant current through the traction motors.

Where service is such that the cars may be operated frequently at maximum speed it is desirable to prevent the repeated use of dynamic braking at loads beyond safe operating limits of the traction motors. As these limits are characterized by an almost constant value of armature voltage, a voltage-limit relay *VLR* may be provided to control the rheostatic-controller pilot motor in a manner similar to relay *ABR*, but responsive to a predetermined traction-motor overvoltage. The effect of voltage-limit operation on braking sequence is indicated by the broken line at the top of Figure 5.

Multiple-Unit Operation

Figure 7 shows one of a number of multiple-unit rapid-transit "compartment" cars to which the electric equipment of a PCC car has been adapted. Each compartment car is equivalent to two PCC cars in electrical equipment, as each car has four two-axle trucks supporting three articulated body sections designated *A*, *B* and *A*1. The outer sections, *A* and *A*1, are identical and carry the major portion of the electrical equipment. The car weight per motor is approximately 15 per cent greater than that of a standard PCC car but, as the car operates in less frequent stop service, is well within the capacity of the motors.

Multiple-unit operation requires that certain devices (controlled mechanically by the operator of a PCC car) be arranged for remote control. The hand-operated reverser of the PCC car is replaced by a conventional electropneumatic reverser, controlled by the energization of either one of two train wires. On a PCC car, the accelerating and braking currents are controlled by varying the tension of the accelerating- and braking-relay restraining spring. For remote control, the spring is fixed at a value which will normally cause the relay to operate at the desired maximum values of current. Lower operating currents are obtained by energizing a separate coil section, *ABR* (*d*) of Figure 6, at various currents to assist the other coils in closing the relay contacts. A variable voltage is supplied the train wire to which the rate coils are connected by a potentiometer incorporated in the master controllers.

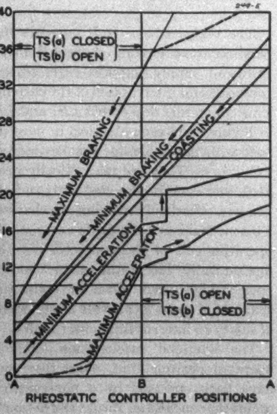

Figure 5. Typical sequence diagram

It is neither practicable nor safe to control all of the circuits directly through train wires from the master controllers, because

(*a*) Some circuits must be energized to obtain dynamic braking sequence.

(*b*) Some circuits associated with undervoltage protection are controlled independently of the master controller.

(*c*) The capacity and number of master controller fingers and train wires would be increased appreciably by direct control.

The control circuits of each equipment are supplied, so far as is practical, from the local battery and its charging generator on each *A* and *A*1 section, through contacts on auxiliary relays which, in turn, are controlled through train wires from the master controllers. Because of the possibility of open circuits or short circuits on these train wires, the circuits are arranged so that it is necessary to energize two or more train wires to release all types of brakes with which the cars are equipped (electric, magnetic track and friction brakes), and also it is necessary to energize two or more train wires to accelerate the cars. Failure of any essential train wire therefore cannot prevent the removal of power and the application of brakes.

Low-Voltage Bus

All control-voltage storage batteries in a train are connected in parallel to provide a reasonable division of load and to insure a reliable supply of power for the master control and train-wire circuits. The battery positives are each connected to the positive bus line through a limiting resistor, which serves the dual purpose of diminishing the effects of inequalities in the setting of the battery-charging generator-voltage regulators and, in case of a fault between positive and negative bus lines, prevents quick depletion of battery charge by limiting the fault current to the full-load capacity of the charging generators.

It is very desirable to ground the battery system to prevent or minimize damage to low-voltage control devices and insulation if a fault should occur between the control circuits and the high-voltage power circuits. Grounding one of the battery bus lines at more than one point in a train, however, invites the diversion of power current from its normal return path through the running rails, if the train should pass over a high-resistance rail bond. In such circumstances the battery negatives, while connected directly to the train wire used as the negative bus line, are also connected to "ground" (car structure) through a resistance such as will limit the current diverted from the rail to the capacity of the bus line and yet pass power-to-control fault currents which may occur.

Remote Battery Disconnect Contactors

To prevent back circuits through contacts in master controllers which are not being operated, all such contacts are usually required to be open when the master controller is nonoperative. But this does not de-energize control circuits local to each equipment which are essential to the operation of brakes. In particular, the magnetic track brakes are fully energized from local batteries and charging generators when all control circuits are opened preparatory to changing control stations or laying the car up in yard or shop. To automatically remove these power loads which are no longer required after the car or train is at standstill, the circuits are supplied through contactors which are closed by energizing a train wire whenever any master controller is operative. Each contactor mechanically latches in the closed position so that the local circuits will not be interrupted if the train wire is subsequently and unintentionally open-circuited. The contactors are automatically tripped open by momentarily energizing another train wire in the sequence of movements the operator must make in making a master controller nonoperative.

Cutting Out Bad-Order Cars

It is of extreme importance that means be provided to permit the continued operation in multiple unit of any equipment in the train which may not be func-

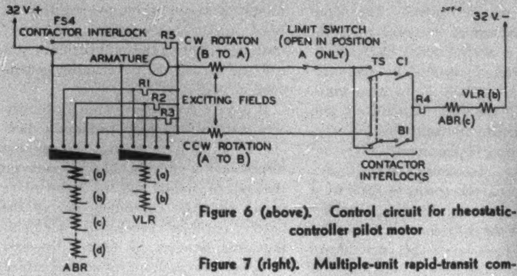

Figure 6 (above). Control circuit for rheostatic-controller pilot motor

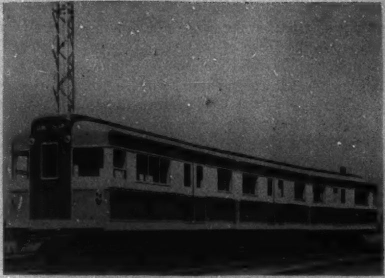

Figure 7 (right). Multiple-unit rapid-transit compartment car

tioning properly. This is particularly true of equipments having motors connected permanently in parallel or having provision for the type of electric braking here described. Failure of a motor reverser to throw to a position in agreement with direction of train movement will cause the motors to build up as short-circuited series generators, the current flowing around the "loop" circuit formed by the motor interconnections. Failure of the braking-circuit contactors to open or the sequence to progress will cause abnormal braking currents to be generated while the rest of the train is being accelerated.

To guard against such contingencies a train-wire circuit is provided which, energized at the rear end of the train, can be completed to the front or operating end to operate a control relay and permit the acceleration of the train, only if all reversers in the train have assumed a position in agreement with the desired direction of train movement, and only if the braking connections of all motor circuits have been interrupted.

Failure to accelerate a train because of operation of this protective circuit requires that the motor "loop" circuit of the defective or "bad-order" car be interrupted by some means. This is accomplished by the operator momentarily energizing a train wire which causes cutout relays in each equipment to operate contacts which, in turn, de-energize the operating coil of a "loop contactor" in the motor circuit of that equipment. The opening of this contactor (designated *LC* in Figure 3) effectively prevents the motors acting as series generators under any condition and, by electrical interlocking, prevents the closing of line contactors on that equipment. The operating coil of contactor *LC* is normally energized directly from the battery-charging generator. As this generator is driven by the motor which also drives a blower for forced ventilation of the resistors and rheostatic controller, loss of ventilation will also cause that equipment to automatically cut out.

The cutout relays, when operated to open all loop contactors, latch in this position. As soon as the operator attempts another acceleration, however, all cutout relays associated with equipments which are functioning properly are tripped out, the loop contactors on all but defective equipments close, and a normal acceleration is made. A cutout relay which remains latched in on a bad-order car also causes a light to burn continuously to assist in location of that car so that it can be readily removed from service for correction of the defect.

Overspeed and Braking Overload Protection

Protection against braking overloads is not desirable or necessary on a PCC car, because loss of dynamic braking due to the operation of an overload relay would mean complete loss of the principal braking means, and, in any case, the current is limited by the loss of adhesion of wheels to the rail.

In the case of multiple-unit compartment cars the loss of dynamic braking on one equipment in a train is not so serious; the greater weight per axle may cause overloads of greater magnitude before the wheels slide on the rail, and the operation of a whole train should not be handicapped by the possibility of repeated overload in one equipment. Accordingly, a braking overload relay is provided which operates to open the loop contactor if a braking current in excess of normal maximum value is obtained. The relay latches in position and may be reset only by hand. The circuits are arranged so that the loop contactor can reclose during each acceleration sequence, and the operation of the braking overload relay, therefore, does not result in loss of accelerating ability.

The possibility of running at sustained high speeds is much more prevalent in rapid-transit service than in street railway service. It is, therefore, desirable to limit (or at least to make less likely) the operation of a train above a predetermined speed which may be determined by any one of several factors. To this end a speed relay is provided which operates when a speed of 40 miles per hour is attained in acceleration. The relay is of the differential type, having a balanced beam attracted to one position by a coil which carries motor-exciting field current, and attracted to another position by a coil which is excited in proportion to motor armature voltage. See Figure 3. At the high motor speed and low current at which the relay is required to operate, the motor is unsaturated, and the ratio of armature voltage to field current is essentially proportional to speed. At the ratio corresponding to 40 miles per hour the relay operates to open the field-shunting contactors and thus reduce the motor tractive effort to a value below the train resistance.

Line Breakers and Overload Protection

The magnitude of fault currents that may occur on circuits connected to the third-rail supply requires extraordinary provisions for interruption of the currents. Line contactors designated *LB*1 and *LB*2 in Figure 3 are each identical in most details with the single line breaker used on PCC cars. They are magnetically operated, and each is provided with an independent overload tripping mechanism. This mechanism not only de-energizes the operating coil of the contactor but also mechanically forces the main contacts open at high speed. Two line contactors are used in series to provide a high factor of safety in interrupting power faults.

The overload mechanisms latch in operated position, but, since they may have been operated by a nonrepetitive occurrence such as an abrupt rise in supply voltage, they may be remotely reset by

Utilization Voltages

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UTILIZATION voltage is usually thought of in terms of the voltage rating of the lamps used, or of a nominal standard voltage announced by the company rendering service. It is probably quite generally understood that the voltage actually appearing at the outlets to which lamps or appliances are connected varies somewhat from time to time. What is not so well recognized, however, is the fact that the voltages delivered at customers' outlets on any power system are normally distributed in a characteristic pattern through a band of voltage of considerable width. It is the purpose of this paper to point out that this voltage band or spread is an inherent characteristic of the system, to discuss its source and its nature, and to indicate the connection between it and the design and rating of utilization equipment.

The voltage which will be treated in this discussion is the "utilization voltage" as distinguished from "service voltage." That is, it is the voltage appearing at wall outlets and lamp sockets rather than that which the utility supplies at the service entrance to the building. The difference between these voltages is the voltage drop in the building wiring, which is an appreciable amount, as will be shown.

While utilization equipment is built for various voltage classes, the major portion is applied at voltages in the 120-volt group. Attention will be given to these first, therefore, but mention will be made later of the special problems encountered with the higher voltages.

Voltage Spread

It would be preferable if the general conception of utilization voltage could be that of a band or spread of voltages between defined limits, rather than of a single voltage such as 115 volts or 120 volts. It is, of course, commonly accepted that a piece of commercial utilization equipment may be called upon to operate at other voltages than the rating stamped on its name plate. There seems to be a tendency, however, to consider such variations as deviations from a normal voltage rather than as themselves normal voltages within a normal voltage band. It should be clearly understood that on any ordinary distribution system such a voltage spread is inherent in its operation. At heavy-load periods, the voltage delivered at customers' outlets will, of necessity, be distributed throughout a band which is rarely less than 6 or 8 volts where close regulation is feasible, and may be as wide as 15 volts in some areas.

With reference to utilization equipment, this means that the equipment is expected to operate satisfactorily to the users through this band of voltage on any individual system, and through a still wider spread of voltage in general when the variations between systems are taken into account. Only a part of this equipment will actually operate regularly at its rated voltage, the rest being used as voltages ranging from the lowest to the highest limits of the spread. It follows, therefore, that, in the design of such equipment, the emphasis must be placed as far as possible on having good operating characteristics over a specified band of voltage rather than at some one voltage. In order that this voltage band be so placed as to be most advantageous to the users, it is essential that a generally accepted standard voltage spread for the industry be fixed and defined.

A considerable amount of work has been done by the transmission and distribution committee and the electrical equipment committee of the Edison Electric Institute during the past few years toward the establishment of standards of utilization voltage, particularly this voltage spread for satisfactory operation. Surveys were made of 45 operating companies to obtain data in regard to the actual operating voltages in use. The data from these surveys, and other information on this subject which has been presented to these committees, have been made available to the author for use in the following discussion.

The reasons for the normal voltage spread will first be described. The numerical order of existing spreads and their limits and the standards which have been proposed will then be discussed. Following that, there will be pointed out for some of the commoner types of equipment, the conditions of operation to which weight should be given in choosing their "best voltage."

Causes of Voltage Spread

There are, in general, three causes for the existence of a "spread" in utilization voltage. The first two pertain to the variation between different power systems throughout the country. The third is responsible for the deviations in any one system or circuit. All three should be given careful consideration in setting a voltage standard or an apparatus rating.

1. Lamp-Voltage Ratings

It is well known, of course, that lamps are made in several standard-voltage ratings, 110, 115, 120, 125, 130 volts, and so on. This and an even greater variety of ratings came into existence in the early growth of the industry. At first this was largely on account of the inability of the makers of carbon lamps to standardize their product to definite voltage ratings.

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the operator momentarily closing a control switch provided for this purpose. Operation of an overload relay does not prevent obtaining normal dynamic braking.

Summary

Operating the series motors of a street railway car as series generators is a convenient way of providing electric braking, but, since the motors are self-excited while braking, they are very sensitive to changing speed and load conditions. This is particularly true of modern small high-speed motors which operate at relatively low-flux densities. Control systems are available and in use, however, which provide a smooth, responsive and flexible electric brake which has replaced the air brake as the principal means of braking modern cars in street railway service.

The use of such a control system with electric braking has been extended to multiple-unit rapid-transit cars by adapting the equipment of the PCC car to conform to the requirements of remote control without sacrifice in safety or reliability, and by providing special features in recognition of the distinctive operating conditions encountered in rapid-transit service.

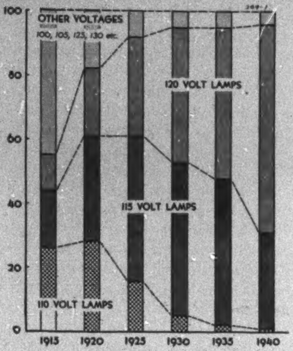

Figure 1. Proportion of incandescent-lamp sales, according to voltage rating

Later, as manufacturing control improved, it was continued on account of the same sort of voltage spread on operating systems which is being discussed here. As time went on there was a tendency toward concentration on a few definite ratings. This was facilitated by the fact that the negative resistance characteristic of tungsten lamps permitted a lamp to be used satisfactorily through a wider spread of applied voltage than was allowable with the positive characteristics of carbon lamps. Figure 1 shows how the lamp sales have varied in proportion to voltage rating since 1915. It is of interest that, whereas 115-volt lamps were in the majority from 1920 to 1930, they are now only 30 per cent of the total, while 120-volt lamps now constitute 65 per cent; 110-volt lamps have become a negligible factor; 125-volt and 130-volt lamps are still insignificant, being more or less special. It is evident, however, that there are a sufficient number of both 115- and 120-volt lamps in use to account for a spread of voltage between different systems which use them, even if the other ratings are not considered.

The shift from 115- to 120-volt lamps has been due to several causes, among them the desire to gain the advantages of higher voltage in distribution and the introduction in recent years of the 120/208-volt three-phase four-wire secondary network. Motor ratings have remained at 110 volts until recently when the standard for single-phase motors has been raised to 115 volts in the National Electrical Manufacturers' Association Standards and in the proposed revision of American Standards Association Standard *C*50. Other utilization appliances have been variously rated at voltages from 110 to 120.

It is believed desirable to discourage a future repetition of this shift in voltage preference to an eventual predominance of one of the higher-voltage lamps. There is now considerable confusion in the ratings and applications of both lamps and appliances. A continued rise in lamp voltages would add to the confusion, particularly in regard to the appliances. These must attempt to cover the whole field of voltages in use but cannot follow changes so readily, their useful life being much longer than that of lamps. The establishment of a standard voltage spread would tend to restrict further extension of that spread upward, unless the advantages were to become obvious and generally acceptable.

2. Relation of Rating to Outlet Voltage

In addition to the differences in lamp- (and system-) voltage ratings, there exists in the industry well founded differences of opinion in regard to the relation which the utilization voltage should bear to the rating of the lamp used. Figure 2 shows the characteristic variation in lamp lumens, energy consumption, and lamp life with variation in applied voltage. Some operators prefer to have the lamps operate as nearly as possible at their voltage rating. Others believe that the increased life resulting from voltages a little under rating is of more practicable value than the higher illumination, and shorter life which results from operation at rated voltage or higher. Others take the opposite view, that the increased lumens per watt at over-voltage operation are preferable. The question is basically one of economy, involving the relation between the cost of lamps and the cost of electrical energy. It must be considered, however, that the nuisance of too frequent lamp replacement may outweigh economy in the minds of the general run of customers.

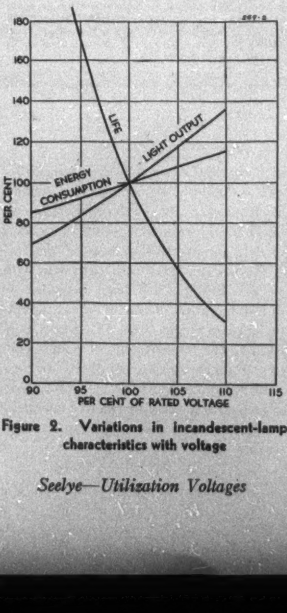

Figure 2. Variations in incandescent-lamp characteristics with voltage

These preferences must, of course, take into account the inherent voltage spread that exists on every system, which will be discussed in detail later. Where, for example, there is a 10 per cent total spread, and this is not uncommon, there will be some customers, even though a small proportion of the total, getting five per cent above the average. If this "average" is held at lamp rating, say 120 volts, these high customers will be getting 126 volts with a lamp life of 55 per cent. If the average is above rating, say 124 volts, the high customers will be getting 130 volts, with lamp life at 37 per cent. The performance of ordinary appliances, such as toasters, waffle irons, and the like, at such voltages must also be considered. Of course, where a smaller spread such as six per cent is feasible, an average of 124 would result in a high of only 127.6.

On the other hand, if the average voltage is low, the customers at the bottom of the spread may be getting inferior illumination and indifferent operation of appliances. It will not be attempted here to state the proper relation between rating and average operating voltage. It is merely pointed out that the voltage spread, including the top and bottom limits, should be taken into account as well as the mid-point or average. The differences in opinion in regard to this relation of lamp rating to voltage leads to a voltage difference even between systems using the same lamp rating. One such system will carry its voltage spread at a higher level than will another.

A confusion in system ratings also results from this source, since these ratings are generally 115-volt or 120-volt, according to the lamp rating used. Some so-called 115-volt systems, with average of outlet voltages above the lamp rating, actually have practically the same voltage spread and at the same voltage level as other systems normally designated at 120 volts but having average outlet voltages below the lamp rating. System rating is, therefore, not a definite indication of the existing utilization voltage.

3. Inherent Voltage Spread in Distribution System

The normal, inherent spread of utilization voltage on an operating system is probably the least understood of the three causes of voltage deviation. Its basis is simple, lying in the elementary fact that when electric current passes over a wire, there is a corresponding drop in voltage. The energy delivered to customers on a power system passes over many miles of wire in

(*a*) Transmission.

(*b*) Step-up and step-down transformers.

(*c*) Distribution circuit feeders and primaries.

(*d*) Distribution transformers.

(*e*) Distribution secondaries and service drops.

(*f*) Customer's house wiring.

The voltage drop in transmission is usually controlled to considerable extent by variation of generator voltage, transformer taps, and regulators. Since any of these can regulate in bulk only at one point, there will be a variation in the voltage between that regulating point and other points on the system at which load is supplied. This variation can be compensated for at the substation by voltage regulators on the substation bus or on individual distribution circuit feeders.

These substation regulators and supplementary regulators and boosters out on the circuits, can also compensate for *part* of the drop in the outgoing distribution circuits. A fairly constant voltage can be held at *some point* out on such a circuit. Since the circuit may consist of several miles of line, however, it is impossible to have the voltage constant at all points. Some customers will be connected near the regulated point and some far from it. Some will be connected through heavily loaded distribution transformers and some through transformers only lightly loaded. The impedance of the transformers themselves will vary with size, type, and make. Some customers on a secondary main will be near the transformer and some will be at the end of long secondaries. Figure 3 is a schematic diagram indicating these conditions. The result is that, even though the voltage at the circuit feeding point is held practically constant throughout the day and night, utilization voltage at the customer's outlets will vary through a band which is of appreciable width.

In Figure 3 the station bus is, for convenience, assumed constant at 122 volts at all loads. The circuit regulator is compensated to hold 122 volts at an intermediate point on the primary mains, indicated by the middle distribution transformer (*C*). The voltage drops indicated for primaries, transformers, secondaries, and service and house wiring are typical, rather than extreme cases, of those in ordinary practice. It is evident that customers' outlet voltages at time of heavy load will be distributed through a band whose maximum is about 122 volts at customer (*A*), and whose minimum is something like 112 volts at customer (*B*). At light-load periods, the voltages lie in a much narrower band, of perhaps 119 to 121, while at no load, which seldom occurs, all would be 122, the voltage at the regulating point (*C*).

Table I

	Maximum Voltage Drop, Per Cent	
	Urban Customers	Rural Customers
In primaries	1.3 to 3.5	3.3 to 10.0
In distribution transformers	2.5 to 4.65	2.0 to 4.4
In secondaries	2.0 to 4.5	No secondaries
In service drops	0.5 to 1.25	0.5 to 1.8
In customer's building wiring	1.0 to 5.0	1.0 to 2.0
Total utilization-voltage spread	7.5 to 12.0	6.87to15.0

There are many sorts of distribution circuits, ranging from those in concentrated urban areas where the distances are short, and regulation can be reasonably close, to rural lines with long distances and correspondingly poorer regulation. In many of the latter, the desirability and practicability of giving reasonably good regulation, similar to that in more densely built suburban territory, is recognized, but, on the other hand, there are others where regulating equipment is omitted in the interest of lower first cost. Also, there are various types of circuits, such as networks, ring-main, and feeder and branch. In any case, however, there will be an unavoidable spread in the delivered utilization voltage similar to that indicated in Figure 3. It may, of course, be considerably less in some areas than the amount shown here (networks for example); and in others it will be greater. It will be located at several different voltage levels on different systems.

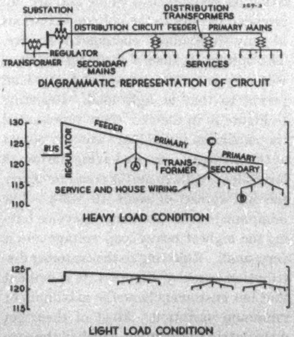

Figure 3. Diagrammatic illustration of voltage spread on a typical distribution circuit

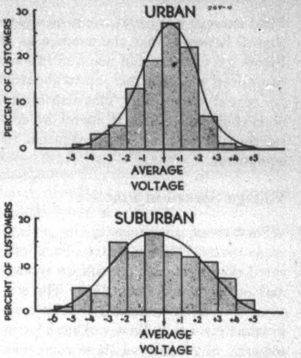

Figure 4. Distribution of customers' outlet voltage

From actual sampling survey of a large system

A survey of 14 companies made by the transmission and distribution committee, Edison Electric Institute, produced results which shed some light on the magnitude of the various elements in this voltage spread. The figures in Table I show the range of values reported. Since these figures were mostly based on design values rather than field measurements, they should be considered only as indicative.

Pattern of Voltage Spread

The outlet voltages on a system are, of course, not distributed uniformly over the voltage spread. The pattern of this distribution is shown in Figure 4. The blocked graphs were obtained from a sampling survey on a large diversified system. While the urban graph is somewhat different from the suburban, their general shape is similar as shown by the smooth, generalized curves. This pattern has been confirmed in essentials by data from several companies and is believed to be typical.

It is evident that comparatively few customers get either the highest or the lowest voltage in the spread, but on the other hand, there are appreciable numbers getting a volt or two higher than the minimum and a volt or two lower than the maximum. The largest number getting any one voltage is about 27 per cent in the urban curve and only about 17 per cent in the suburban.

The average voltage is determined by the shape of the curve and this may vary somewhat between different systems.

Field surveys in several companies have shown, however, that the average as in Figure 4 is near the mid-point, or in some cases even a little higher, up to about 60 per cent of the spread. This distribution of customers' voltage is useful in considering the range for "best operation" of equipment.

Voltage Spread in Practice

Surveys of the industry in general, made by the EEI committees, have indicated existing values for voltage spreads and other related quantities. The surveys covered 45 companies, serving approximately ten million residential customers. Since most of these companies did not have available figures on actual field surveys of voltage conditions, design values were reported. Some allowance should perhaps be made for discrepancy between these and voltages which really exist.

No extensive field data on the voltage drop in building wiring is available. Some estimates are given in Table I. A sampling survey on the Detroit Edison system covering 300 customers, mostly residential, showed a range from about $^1/_2$ volt to 5 volts, with an average of about $1^1/_4$ volts to lamps in the most used room under evening load conditions. There are, of course, some outlets which have less drop than these, and there will be some cases with more. It is believed that a 3-volt drop will cover the maximum for the great majority of customers.

If 3 volts is added to the spread at service entrance resulting from the survey as listed above the following deductions can be drawn:

1. The maximum voltage spread at *utilization outlets* for the industry as a whole is of the order of 20 volts, from 107 volts (110−3) to 127 volts.
2. This whole spread will not occur ordinarily in any one system but takes account also of the variation between systems which has been previously discussed. It is the spread for which utilization equipment in general must be designed.
3. There will no doubt be some outlets outside of this spread, both above the upper limit and below the lower limit. It is believed, however, that the percentage of these will be very small, and that they may be considered as unusual or temporary conditions, for which operation of equipment cannot be expected to be as good as it should be within the normal range.
4. The greater bulk of customers have outlet voltages within the band between 110 (113−3) and 125 volts.
5. On individual systems the voltage spread at utilization outlets ranges from 7 volts to 18 volts, with nearly two thirds lying between 10 and 13 volts, very few above that, and the remaining one third lying between 7 and 9 volts.

Table II. Service Voltage

Drop in building wiring should be deducted

Minimum Service Voltage Reported	110 volts
Maximum Service Voltage Reported	127 volts
Minimum Service voltage for companies serving 77 per cent of the total customers	113 volts
Maximum Service voltage for companies serving 80 per cent of the total customers	125 volts
Voltage spread at service	
Minimum reported	4 volts
Maximum reported	15 volts
Classification of service voltage spreads	
For companies reporting 33 per cent of total customers	4 to 6 volts
For companies reporting 62 per cent of total customers	7 to 10 volts
For companies reporting 5 per cent of total customers	11 to 15 volts

It should be clearly understood that these figures refer to voltage spreads existing on systems as a whole and are not intended to refer to the voltage variations at any individual customer's service. The voltage at such a customer's service will vary from his voltage at heavy-load period to that at light-load. Referring to Figure 3, in the case there represented the maximum individual variation occurs at the customer's service having the lowest heavy-load voltage and is equal to nearly the full spread of some 10 volts. The minimum is at the customer's service having the highest heavy-load voltage and is very small. Referring to the customer distribution curve of Figure 4, it is evident that few customers have the maximum or minimum variation. Most of them get more nearly the average, which in the case of Figure 3 would be about 5 volts.

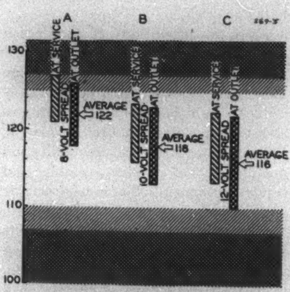

Figure 5. Typical voltage spreads within the proposed standard

A—Urban service, closely regulated, 120-volt lamps

B—Urban service, general, 120-volt lamps

C—Rural service, 115-volt lamps

The figures which have been cited lead to a suggestion of a general standard maximum utilization voltage spread of 107 to 127 volts, with a *preferred* spread of 110 to 125 volts. Figure 5 illustrates how several different typical service conditions can fall within this spread. There are, of course, many other variations which can and will be used on operating systems and still lie within the total proposed spread.

Higher Voltages

The discussion so far has referred only to voltages in the 120-volt group. The same problem exists, however, with the higher utilization voltages, 240, 480, 2,400, and so on. In the 240-volt group, the complication of the "odd-ratio" system is introduced. Network distribution in dense urban territory is now commonly four-wire, at 120/208 volts. The voltage supplied to 240-volt equipment on such a system is only 87 per cent of what it would be on a 120/240-volt even-ratio system, with the same voltage for the 120-volt equipment in both cases.

For the most part, the same standard 240-volt equipment has been used satisfactorily on these odd-ratio systems as on those of even-ratio. Special 208-volt motors are available but have not been widely adopted. This practice has been allowable, largely because the odd-ratio systems usually are found in densely loaded areas where service voltage tends to be relatively high and the voltage spread relatively small, making the minimum voltage high enough for 240-volt equipment. There are several advantages in having one standard line of apparatus so designed as to serve both purposes if feasible. Manufacturing and stocking are obviously simpler for one line than for two; interchangeability is provided between locations where service voltage is odd-ratio and locations where it is even-ratio.

In setting up a standard voltage spread for the industry, it is desirable that it be broad enough to be adequate for both even-ratio and odd-ratio systems, and yet not too broad to allow one line of apparatus to cover it. Taking into consideration the probability that the voltage drop to 240-volt equipment would be somewhat greater than to that at 120 volts, and that the service voltage in general to 208-volt services would be relatively high and well-regulated, a spread of 193 to 250 volts maximum, 197 to 245 volts *preferred* has been recommended.

At the still higher voltages, 480, 600, etc., similar problems of "odd-ratios" are also found, and it is probable that the voltage spreads for these levels should be multiples of those at the 240-volt level.

Equipment Design

One of the most important objectives in trying to establish standard voltage spreads is the simplification of the design and rating of utilization equipment. If the voltage spread which must be met in practice is fixed between reasonable limits, there will be more assurance that equipment which operates satisfactorily within those limits will be generally satisfactory for the industry. Also, as further knowledge is gained of the characteristics of service voltage within those limits, the designs of specific items of apparatus can be more definitely directed toward the actual voltages at which they will most commonly be operated.

This paper has pointed out the fact that a voltage spread of from 7 to 15 volts is a normal condition on an operating system. The utilization equipment supplied should be expected to operate at any voltage throughout that spread, with satisfactory results to the user. If and when a standard spread for the industry, such as that which has been suggested, is generally adopted, it should be expected that equipment sold would operate satisfactorily *throughout* that spread. Since the bulk of the customers will have voltages within a somewhat smaller spread than the maximum, the preferred voltage spread indicates the range through which *good characteristics* of operation should be maintained. On the other hand, it should be kept in mind that there will be occasional customers on any system where voltage will fall somewhat above or below a recognized spread, and their equipment should still operate, even if not with characteristics which would be considered generally satisfactory.

It would be desirable, if practicable, to eliminate all the variety of voltage ratings on different equipment, reducing them to a uniform rating of, perhaps, the nominal 120-volt designation, with the understanding that they would meet the requirements of the standard-voltage spread. It is realized that there are difficulties in carrying out such a simplification, one of them being the different ideas which are prevalent concerning the proper relation between lamp rating and average voltage supplied, or in other words, between lamp life and illumination. Another is the probability that all types of equipment are not adaptable to as wide a range of voltage as the spreads proposed. These considerations will probably require the retention of more than one rating for some equipment. Other items, such as motors, are probably already pretty well adapted to such a standard range and could be uniformly rated accordingly.

While it is not the province of this paper to attempt to specify the best voltage for which any piece of equipment shall be designed, some of the considerations which could affect such a choice within a standard voltage spread will be suggested.

LAMPS

There is probably a distinct need for both "long-life" lamps, and for "high-illumination" lamps. The former would be used where efficient illumination is not the prime requirement, such as for low-wattage lamps for indicating or ornamental purposes. The design voltage should be high in relation to the voltage spread so that they would be operated at undervoltage most of the time. "Long-life" lamps in larger sizes would also be preferred for general use by some operators. For this purpose, however, the design should give somewhat more regard to illumination and less to extreme length of life. "High-illumination" lamps would have a demand where illumination intensity is closely measured, and the application is strictly on a "lumens-per-dollar" basis. The design voltage should be nearer the middle of the spread or below it, giving "normal" voltage or "over-voltage" operation to the majority of lamps.

FLUORESCENT LAMPS

A recent publication states, "The lamps with 'low'-voltage ballast equipment are designed for operation on circuit voltages of from 110 to 125 volts inclusive and, in some cases, may operate satisfactorily on circuits as low as 105 or as high as 130 volts."

These figures correspond well with those of the proposed standard-voltage spread.

ELECTRIC-RANGE ELEMENTS

Ordinarily the voltage drop to a major appliance, such as a range, will be fairly large, due to the current it draws. Much of its operation will be during hours which are off-peak for the lighting load, but it will also have to operate on peak. Its maximum, will, therefore be several volts lower than the top of the voltage spread and its minimum near the bottom of it. The suggested standards, published in 1938 by a joint EEI–NEMA committee, have a "preferred standard rating" of 115–120 volts, based on a design voltage of 118 volts, with a maximum operating voltage of 124 volts. There are, in addition, two optional ratings, one at 125 volts with maximum operating voltage of 129 volts, and the other at 110 volts with a 116-volt maximum. The preferred rating corresponds fairly well with the proposed 110–125-volt preferred spread.

TOASTERS, WAFFLE IRONS, FLATIRONS, AND SIMILAR APPLIANCES

These produce a considerable voltage drop, due to their own relatively large current, but operate mostly offpeak, when supply voltage is relatively high. Their maximum will, therefore, be several volts below the top of the voltage spread and their minimum several volts above the bottom of it.

REFRIGERATOR MOTORS, OIL-BURNER MOTORS, AND SIMILAR MOTORS

These must operate throughout the day and hence throughout the voltage spread, but most of the operation during 24 hours will be at voltages in the top part of the spread, since such voltages exist during the greater number of hours.

It is not intended to infer that the existence of normal voltage spread or of the factors involved in "best voltage" has been unknown to the makers of utilization equipment or has been ignored by them. It is believed, however, that the material which has been presented here will be helpful to both makers and users of such equipment in promoting a better understanding of the nature of the voltage spread and the related numerical values of utilization voltage.

Current Loci for the Capacitor Motor

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MEMBER AIEE

THE basic theory of the capacitor motor from the cross-field point of view has been recently presented by Puchstein and Lloyd.[1] It is the purpose of this paper to extend the basic relations developed by them so as to demonstrate certain current-loci characteristics of the capacitor motor.

The circuit to be considered is shown in Figure 1. A variable condenser of impedance Z_C is connected in series with the auxiliary winding. The rotor squirrel-cage winding is considered equivalent to a d-c winding with the commutator brushes arranged to short circuit the winding along the main and auxiliary axes of the stator winding. A common voltage is impressed on the two windings.

The flux relations are illustrated in Figure 2. There is a mutual flux linking the stator and rotor windings along each axis of the machine. Considered as a transformer there are leakage fluxes linking with each of the stator windings, and also leakage fluxes which link the rotor winding on the main and auxiliary axes. Each of these fluxes is considered to be stationary in space and variable in time. As a consequence the mutual fluxes and rotor-leakage fluxes are cut by the rotor conductors as they rotate.

Current and voltage relations are indicated by the vector diagrams of Figure 3. The vector diagram for the auxiliary axis is drawn so that the mutual flux ϕ_{mb} is in space quadrature with the main-axis mutual flux ϕ_{ma}, and is lagging. This makes it possible to represent each of the speed voltages in its proper phase relation. In each stator winding there is a local-impedance drop and a component of impressed voltage equal and opposite to the rotor-induced voltage, assuming a ratio of transformation of unity for each axis. In each rotor circuit there is an induced voltage, a local-impedance drop, a generated voltage due to cutting the mutual flux of the other axis, and a generated voltage resulting from cutting the rotor-leakage flux of the other axis. Applying Kirchoff's law of voltages to each circuit on the main and auxiliary axes the following equations can be written:

Main Axis (Figure 3a)

(Stator) $$V=I_aZ_a+(I_a-I_{2a})Z_{ma} \qquad (1)$$

(Rotor) $$O=-(I_a-I_{2a})Z_{ma}+I_{2a}Z_2+ jS\left[-\frac{Z_{mb}}{k}(I_b-I_{2b})+jkX_2I_{2b}\right] \qquad (2)$$

Auxiliary Axis (Figure 3b)

(Stator) $$V=I_b(Z_b+Z_c)+(I_b-I_{2b})Z_{mb} \qquad (3)$$

(Rotor) $$O=-(I_b-I_{2b})\frac{Z_{mb}}{k}+kZ_2I_{2b}- jS[-Z_{ma}(I_a-I_{2a})+jX_2I_{2a}] \qquad (4)$$

wherein

V = applied or line voltage
I_a = current in main stator winding
I_b = current in auxiliary stator winding
I_{2a} = rotor current in main-axis circuit
I_{2b} = rotor current in auxiliary-axis circuit
$Z_a=r_a+jX_a$ = local impedance of main stator winding
$Z_b=r_b+jX_b$ = local impedance of auxiliary stator winding
$Z_{ma}=jX_{ma}$ = mutual inductive impedance of main-axis stator and rotor windings
$Z_{mb}=jX_{mb}$ = mutual inductive impedance of auxiliary-axis stator and rotor windings
$Z_2=r_2+jX_2$ = local impedance of each rotor circuit
$Z_c=r_c-jX_c$ = impedance of capacitance in series with auxiliary winding
k = ratio of auxiliary-winding turns to main-winding turns
S = speed as a decimal fraction of synchronous speed

From equations 1 and 3

$$\left.\begin{aligned} I_{2a}&=\frac{I_a(Z_a+Z_{ma})-V}{Z_{ma}} \\ I_{2b}&=\frac{I_b(Z_b+Z_c+Z_{mb})-V}{Z_{mb}} \end{aligned}\right\} \qquad (5)$$

Substituting these relations into equations 2 and 4 find

$$\left.\begin{aligned} A_aI_a+B_aI_b&=K_aV \\ A_bI_a+B_bI_b&=K_bV \end{aligned}\right\} \qquad (6)$$

wherein,

$$A_a=Z_a+Z_b+\frac{Z_aZ_2}{Z_{ma}} \qquad (7)$$

$$A_b=S\left[X_2\left(1+\frac{Z_a}{Z_{ma}}\right)-jZ_a\right] \qquad (8)$$

$$B_a=S\left[(Z_b+Z_c)\left(j\frac{1}{k}-k\frac{X_2}{Z_{mb}}\right)-kX_2\right. \qquad (9)$$

$$B_b=kZ_2+(Z_b+Z_c)\left(\frac{1}{k}+k\frac{Z_2}{Z_{mb}}\right) \qquad (10)$$

$$K_a=1+\frac{Z_2}{Z_{ma}}+S\left(j\frac{1}{k}-k\frac{X_2}{Z_{mb}}\right) \qquad (11)$$

$$K_b=S\left(-j+\frac{X_2}{Z_{ma}}\right)+\left(\frac{1}{k}+k\frac{Z_2}{Z_{mb}}\right) \qquad (12)$$

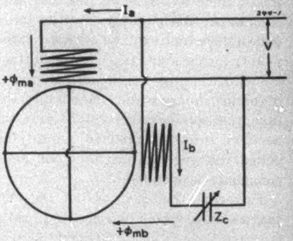

Figure 1. The circuit

In each of these equations the complex expressions for the impedances must be substituted (see nomenclature). When the substitutions have been made find

$$A_a=\left(r_a+r_2+\frac{r_aX_2+r_2X_a}{X_{ma}}\right)+ j\left(X_a+X_2+\frac{X_aX_2-r_ar_2}{X_{ma}}\right) \qquad (13)$$

$$A_b=S\left[X_2+X_a\left(1+\frac{X_2}{X_{ma}}\right)\right]- jSr_a\left(1+\frac{X_2}{X_{ma}}\right) \qquad (14)$$

$$B_a=-S\left[kX_2+(X_b-X_c)\left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right)\right]+ jS(r_b+r_c)\left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right) \qquad (15)$$

$$B_b=\left[kr_2\left(1+\frac{X_b-X_c}{X_{mb}}\right)+(r_b+r_c)\times \left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right)+j\left[kX_2+(X_b-X_c)\times \left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right)-\frac{kr_2}{X_{mb}}(r_b+r_c)\right] \qquad (16)$$

$$K_a=\left(1+\frac{X_2}{X_{ma}}\right)+j\left(\frac{S}{k}+\frac{kSX_2}{X_{mb}}-\frac{r_2}{X_{ma}}\right) \qquad (17)$$

$$K_b=\left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right)-j\left[\frac{kr_2}{X_{mb}}+S\left(1+\frac{X_2}{X_{ma}}\right)\right] \qquad (18)$$

Solving the equations of (6) simultaneously,

$$\left.\begin{aligned} I_a&=\frac{B_bK_a-B_aK_b}{A_aB_b-A_bB_a}\cdot V \\ I_b&=\frac{A_aK_b-A_bK_a}{A_aB_b-A_bB_a}\cdot V \end{aligned}\right\} \qquad (19)$$

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Substitution in the equations of 19 from equations 13–18 inclusive yields

$$\left.\begin{aligned} I_a&=\frac{M+jN}{U+jW}\cdot V\\ I_b&=\frac{P+jQ}{U+jW}\cdot V \end{aligned}\right\}\qquad(20)$$

wherein,

$$M=\frac{1}{k}\left[(r_b+r_c)\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)+\frac{r_2(X_b-X_c)}{X_{ma}}\right]+k\left[r_2\left(1+\frac{2X_2}{X_{ma}}\right)+\frac{X_2}{X_{mb}}(r_b+r_c)\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)+\frac{r_2}{X_{mb}}(X_b-X_c)\left(1+\frac{2X_2}{X_{ma}}\right)-\frac{(r_b+r_c)r_2^2}{X_{ma}X_{mb}}\right]\qquad(21)$$

$$N=\frac{1}{k}\left[(X_b-X_c)\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)-\frac{r_2(r_b+r_c)}{X_{ma}}\right]+Sr_2+k\left[X_2\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)-\frac{r_2^2}{X_{ma}}-\frac{r_2}{X_{mb}}(r_b+r_c)\left(1+\frac{2X_2}{X_{ma}}\right)+\frac{X_2}{X_{mb}}(X_b-X_c)\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)-\frac{(X_b-X_c)r_2^2}{X_{ma}X_{mb}}\right]\qquad(22)$$

$$P=\frac{1}{k}\left[r_2+r_a(1-S^2)+\frac{r_2X_a+r_aX_2(1-S^2)}{X_{ma}}\right]+k\left[\frac{r_aX_2(1-S^2)+r_2(X_a+2X_2)}{X_{mb}}+\frac{r_aX_2^2(1-S^2)+r_2(2X_aX_2-r_ar_2)}{X_{ma}X_{mb}}\right]\qquad(23)$$

$$Q=\frac{1}{k}\left[(X_a+X_2)(1-S^2)+\frac{X_aX_2(1-S^2)-r_ar_2}{X_{ma}}\right]-Sr_2+k\left[\frac{X_2(X_a+X_2)(1-S^2)-r_2(r_a+r_2)}{X_{mb}}+\frac{X_aX_2^2(1-S^2)-r_2(2r_aX_2-r_2X_a)}{X_{ma}\,X_{mb}}\right]\qquad(24)$$

$$\begin{aligned}U=\frac{1}{k}\Bigg[&(r_b+r_c)\left\{r_2+r_a(1-S^2)+\frac{r_2X_a+r_a(X_2-S^2)}{X_{ma}}\right\}+\\&(X_b-X_c)\left\{\frac{r_ar_2-X_a(X_2-S^2)}{X_{ma}}-(X_a+X_2)(1-S^2)\right\}\Bigg]+\\k\Bigg[&r_2(r_a+r_2)-X_2(X_a+X_2)(1-S^2)+\frac{r_2(r_2X_a+2r_aX_2)-X_aX_2(X_2-S^2)}{X_{ma}}+\\&(r_b+r_c)\left\{\frac{r_aX_2(1-S^2)+r_2(X_a+2X_2)}{X_{mb}}+\frac{r_2(2X_2X_a-r_ar_2)+r_aX_2(X_2-S^2)}{X_{ma}X_{mb}}\right\}+\\&(X_b-X_c)\left\{\frac{r_2(r_a+r_2)-X_2(X_a+X_2)(1-S^2)}{X_{mb}}+\frac{r_2(r_2X_a+2r_aX_2)-X_aX_2(X_2-S^2)}{X_{ma}X_{mb}}\right\}\Bigg]\end{aligned}\qquad(25)$$

$$\begin{aligned}W=\frac{1}{k}\Bigg[&(r_b+r_c)\left\{(X_a+X_2)(1-S^2)+\frac{X_a(X_2-S^2)-r_ar_2}{X_{ma}}\right\}+\\&(X_b-X_c)\left\{r_2+r_a(1-S^2)+\frac{r_2X_a-r_a(X_2-S^2)}{X_{ma}}\right\}\Bigg]+\\k\Bigg[&r_2(X_a+2X_2)+r_aX_2(1-S^2)-\frac{r_2(r_ar_2-2X_aX_2)-r_aX_2(X_2-S^2)}{X_{ma}}+\\&(r_b+r_c)\left\{\frac{X_2(X_a+X_2)(1-S^2)-r_2(r_a+r_2)}{X_{mb}}+\frac{X_aX_2(X_2-S^2)-r_2(r_2X_a+2r_aX_2)}{X_{ma}X_{mb}}\right\}+\\&(X_b-X_c)\left\{\frac{r_2(X_a+2X_2)+r_aX_2(1-S^2)}{X_{mb}}+\frac{r_aX_2(X_2-S^2)+r_2(2X_aX_2-r_ar_2)}{X_{ma}X_{mb}}\right\}\Bigg]\end{aligned}\qquad(26)$$

Substitution of the complex expressions for the impedances in the equations of 5 results in

$$\left.\begin{aligned} I_{2a}&=I_a\left[1+\frac{X_a}{X_{ma}}\right]+j\frac{V-I_ar_a}{X_{ma}}\\ I_{2b}&=I_b\left[1+\frac{X_b-X_c}{X_{mb}}\right]+j\frac{V-I_b(r_b+r_c)}{X_{mb}} \end{aligned}\right\}\qquad(27)$$

The equations of 20 and 27 are better for purposes of extended computation than are those of 19 and 5 because they require a minimum number of operations involving complex numbers. Although most engineers can handle the complex algebra without difficulty, it is generally admitted that the chances of error are reduced if the equations are such as to minimize the number of manipulations requiring complex algebra.

To illustrate the application of the preceding relations, computations have been made for the four-pole one-fourth-horsepower 110-volt single-phase motor for which constants are given in Morrill's[2] paper of April 1929. In Morrill's paper corresponding values of resistance and reactance for the capacitive impedance are given for only two different values of impedance. Here the interpolation curve of Figure 4, which includes the two values, is assumed to represent the variable capacitance used. Other values of the constants for this machine, as recorded by Puchstein and Lloyd,[1] are

$r_a=2.02$ (ohms) $\quad X_a=2.79$ (ohms)
$r_b=7.13$ " $\quad X_b=3.22$ "
$r_2=4.12$ " $\quad X_2=2.12$ "
$X_{ma}=66.8$ (ohms)
$X_{mb}=92.9$ "
$k=1.18$ "

Substitution of these constants into the equations 21–26 inclusive yields

$$\left.\begin{aligned} M&=12.17+0.912r_c-0.109X_c-S^2(6.589+0.914r_c)\\ N&=4.425-0.109r_c-0.903X_c+4.12S+S^2(0.906X_c-5.448)\\ P&=5.853-1.826S^2\\ Q&=4.167-4.12S-4.38S^2\\ U&=47.0+5.88r_c+3.95X_c+S^2(13.52-1.795r_c-4.333X_c)\\ W&=86.2+3.93r_c-5.73X_c-S^2(41.68+4.34r_c-1.79X_c \end{aligned}\right\}\qquad(28)$$

By using these relations in conjunction with the data of Figure 4, computations were made for several values of S. The results of these computations are shown in Figures 5, 6, and 7. It is seen that P and Q are independent of the capacitance, and plot as linear functions of speed over the operating range of speeds. The constants M, N, U and W are seen to be approximately linear functions of the capacity reactance for all speeds over the operating range of capacitances. The curves may be extrapolated linearly. For the practical range of operation P and U are always positive and M, N, Q and W are always negative.

For several constant values of S computations of I_a and I_b were made as functions of X_c using the equations of 20. A plot of these values is shown in Figures 8 and 9. It will be observed that

(*a*) The locus of each current for constant S is a circle.
(*b*) The locus of each current for constant X_c is a circle.
(*c*) The centers of each circular locus lie on the arc of a circle.
(*d*) For I_a the locus of centers of circles of constant S passes through the blocked rotor value of current (Figure 8).
(*e*) For I_b the locus of centers of circles of constant X_c passes through the origin of co-ordinates (Figure 9).
(*f*) For the loci of I_a with constant S the triangles ABC remain similar. A is the blocked-rotor point, B is the extremity of a diameter which passes through A, and C is a point on the locus corresponding to a fixed value of X_c.
(*g*) For the loci of I_b with constant S, the angle δ between a line joining the point on the locus corresponding to a fixed value of X_c to the origin and the diameter which passes through the origin is constant.
(*h*) Over the whole range of speeds ($S=1.0$ to $S=0.0$) the radius of a circular locus of constant S for I_a is very closely equal to S^2.

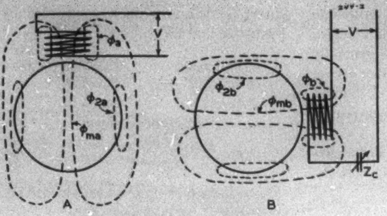
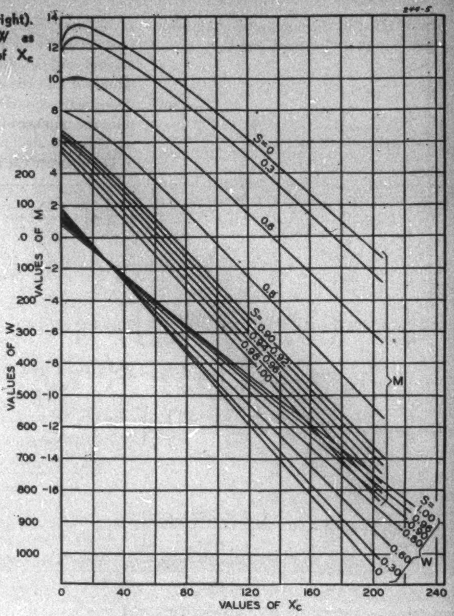

244-2
V
ϕ_a
ϕ_{2a}
ϕ_{ma}
A
ϕ_{2b}
ϕ_{mb}
ϕ_b
V
Z_c
B

Figure 2. The flux relations

A—Main-winding fluxes
B—Auxiliary-winding fluxes

Figure 5 (right). M and W as functions of X_c



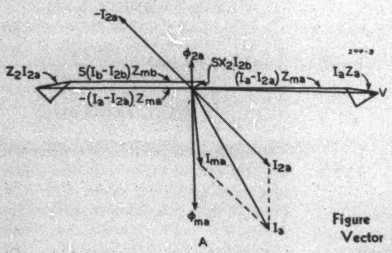
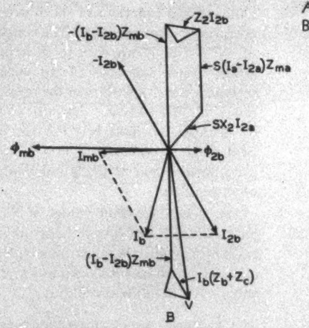

244-3
$-I_{2a}$
ϕ_{2a}
SX_2I_{2b}
Z_2I_{2a}
$S(I_b-I_{2b})Z_{mb}$
$(I_a-I_{2a})Z_{ma}$
I_aZ_a
$-(I_a-I_{2a})Z_{ma}$
V
I_{ma}
I_{2a}
ϕ_{ma}
I_a
A

Figure 3 (left). Vector diagrams

A—Main axis
B—Auxiliary axis


Z_2I_{2b}
$-(I_b-I_{2b})Z_{mb}$
$-I_{2b}$
$S(I_a-I_{2a})Z_{ma}$
SX_2I_{2a}
ϕ_{mb}
I_{mb}
ϕ_{2b}
I_b
I_{2b}
$(I_b-I_{2b})Z_{mb}$
$I_b(Z_b+Z_c)$
V
B

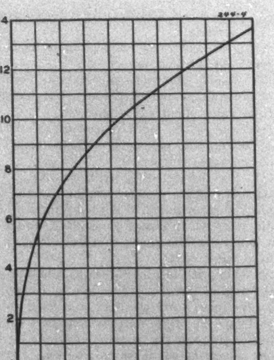
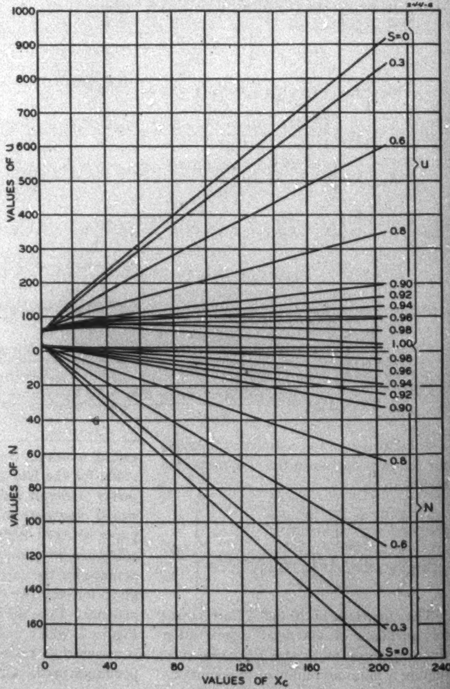


Figure 4 (left). Relation between X_c and r_c

Figure 6 (right). N and U as functions of X_c



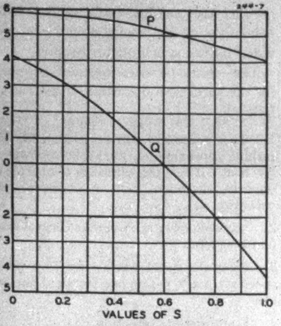


Figure 7. P and Q as functions of S

times the radius of the circle for $S=1$. For I_b the radius is approximately S^2 times the radius of the circle for $S=1$, over the operating range of speeds ($S=1.0$ to $S=0.9$).

As a consequence of these observations the following procedure is suggested for the determination of loci:

(*a*) Compute constants M, N, P, Q, U and W for three widely separated values of X_c, such as $X_c=20$, $X_c=40$, and $X_c=200$, at speeds of $S=1$, $S=0.8$, and $S=0$.

(*b*) Using the constants thus determined find corresponding values of I_a and Ib (any one computation for I_a with $S=0$ will suffice).

(*c*) Plot the computed currents as shown in Figure 8 and Figure 9.

(*d*) Using perpendicular bisectors locate the centers of circles for $S=1$, $S=0.8$ and $S=0$.

(*e*) Using perpendicular bisectors locate the center of the circular arc passing through the centers of circular loci found in (*d*), and draw the arc.

(*f*) On the plot of I_a draw the locus for $S=1$, and then draw the diameter of this circle which, if extended, passes through the current value for $S=0$.

(*g*) Connect the end of the diameter thus drawn, and the blocked-rotor point, to the point on the locus corresponding to the largest value of X_c, and determine the angle β.

(*h*) On the plot of I_b draw the locus for $S=1$, and the diameter which passes through the origin.

(*i*) Measure the angle δ between this diameter and the vector I_b corresponding to the largest value of X_c.

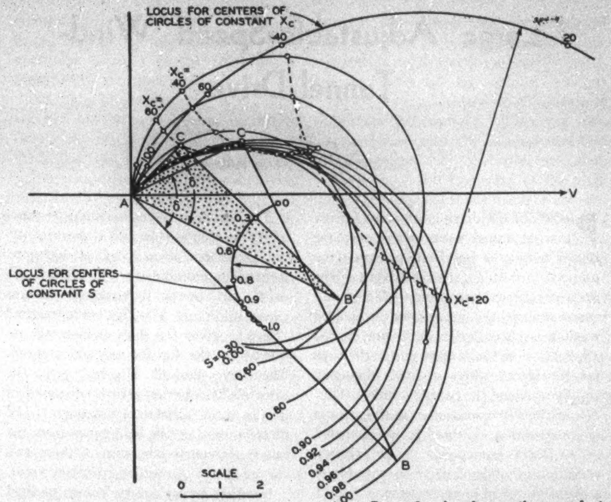

LOCUS FOR CENTERS OF CIRCLES OF CONSTANT X_c
244-9
40
20
X_c= 40
60
X_c= 60
100
C
C
A
V
δ
δ
0
0.3
0.6
0.8
0.9
1.0
B
B
X_c=20
LOCUS FOR CENTERS OF CIRCLES OF CONSTANT S
S=0.30
0.00
0.60
0.80
0.90
0.92
0.94
0.96
0.98
1.00
SCALE
0 1 2

Figure 9. Auxiliary-coil current loci

(*j*) For I_a compute the radii of other circles of constant S between $S=1$ and $S=0.9$ as S^3 times the radius for $S=1$. For I_b use S^2 intead of S^3.

(*k*) By trial find the centers of these circles subject to the conditions that the centers lie on the locus of centers, and triangles ABC remain similar. (The first condition is sufficient for I_b.) Draw the circles.

(*l*) From the loci for $S=1$ determine and plot β and δ as functions of X_c. (Three points are sufficient to determine the curve if it is borne in mind that β changes very slowly with large values of X_c.) From this curve any desired value of X_c on the loci can be found, and any locus of I_a and I_b for constant X_c is readily determined.

(*m*) For each of the values of I_a and I_b found in item (*b*) compute corresponding values of I_{2a} and I_{2b} by the equations of 27.

(*n*) Plot the values of I_{2a} and I_{2b}. The loci are similar to those of I_a and I_b, respectively, but displaced from them by the

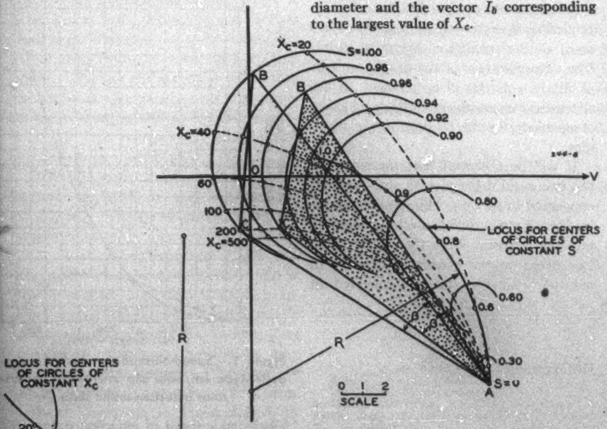

X_c=20
S=1.00
0.98
0.96
0.94
0.92
0.90
B
B
X_c=40
0
60
V
244-8
100
200
X_c=500
C
0.9
0.80
LOCUS FOR CENTERS OF CIRCLES OF CONSTANT S
0.8
0.60
0.6
β
β
R
R
0.30
S=0
A
0 1 2
SCALE
LOCUS FOR CENTERS OF CIRCLES OF CONSTANT X_c
20
40
60
100
200
500

Figure 8. Main-coil current loci

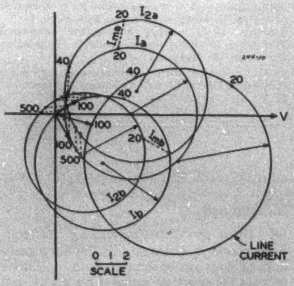

20
I_{2a}
I_{ma}
I_a
20
40
40
40
244-10
20
500
100
V
100
20
I_{mb}
100
500
I_{2b}
I_b
LINE CURRENT
0 1 2
SCALE

Figure 10. Current loci for S=0.96

Large Adjustable-Speed Wind-Tunnel Drive

C. C. CLYMER
MEMBERSHIP APPLICATION PENDING

PROPELLER-drive equipment for use in wind-tunnel work, where airplane model testing is involved, presented no unusual problems until the advent of the present national emergency. The emergency stressed the importance of research work in airplane design, necessitating the application of the largest motor drive so far considered where a fixed frequency supply provided the energy source. Tunnels are now in operation or in the process of construction, powered by drives rated up to 40,000 horsepower. The primary condition for all such drives is variable or adjustable speed over at least a 6 to 1 range, with a large number of speed-control points and accurate speed regulation.

The accepted method of controlling wind-tunnel motors heretofore was by means of adjustable-voltage control using conventional apparatus, or multispeed wound-rotor induction motors with slip regulators. The conventional adjustable-voltage or generator-field-control system is out of the question, for either single or double units in the above capacities. Two-speed wound-rotor induction motors using slip regulators presented formidable design problems, and offered such questionable operating characteristics as to discourage their consideration. Aside from the questionable practice of starting such large motors directly from existing power systems, the problem of disposing of the slip energy is quite serious. It appears that the problem of the dissipation of energy in large quantities is almost as difficult as the problem of producing it.

The drive under discussion requires 40,000 horsepower in two counterrevolving propellers at a speed of 300 rpm. Assuming wound-rotor induction-motor drive, the power requirements are as given in Figure 1. The curve marked "fan hp" gives the shaft horsepower required by the fan for any given speed. The curve marked "slip hp" gives the rotor electrical energy to be disposed of in the most satisfactory manner. The motor input, or the load taken from the line is, of course, the sum of these two curves at any particular operating point.

Needless to say, every known method of speed control was analyzed and its advantages and disadvantages tabulated before the described system was selected. None of the existing systems completely met the conditions; hence, this combination of machines. This arrangement is shown schematically in Figure 2. The two d-c machines were used on each set simply because of design consideration. A smaller drive would use but one such d-c machine.

Obviously the main drive motor can not operate with zero slip except with an unwarranted complication. Accordingly, the main drive motors were selected with a synchronous speed of 327 rpm, but intended to operate only to a speed of 300 rpm, as the maximum operating point. The determination of the proper amount of slip is a matter of economics. If the slip is too low, means must be provided to compensate for the IR drop in the windings.

It will be observed that the rotors of the two main drive motors are, in effect, connected in series. This connection introduces a synchronous machine damping into the circuit. Were the rotors of the two main motors connected directly in parallel with the stators connected in parallel, the machines could oscillate independently of the restraining force provided by the synchronous machine.

The characteristics deemed essential for the successful operation of the tunnel follow:

1. Two propellers will be used, each absorbing approximately 20,000 horsepower at 300 rpm. These propellers are to operate in opposite directions but in absolute synchronism.
2. Variable speed with vernier control from 50 to 300 rpm.
3. Accurate speed control for any given speed setting to within one fourth of one per cent.
4. Power-factor correction.
5. Low-starting kilovolt-amperes.
6. Control of the rate of change of power.

Desirable but not necessarily cardinal characteristics may be listed as follows:

1. High efficiency.
2. Low maintenance cost.
3. Ease of speed control.
4. Negligible line disturbance.

The motors under discussion could be located in the tunnel proper, mounted in a streamlined nacelle or mounted outside the tunnel and the fans operated through shafting. The total input to the stator minus the output from the rotor is dissipated in the tunnel air stream. The motor losses are, of course, dissipated in the nacelle if the motors are tunnel-mounted. Since the tunnel air reaches a rather high temperature, it is not satis-

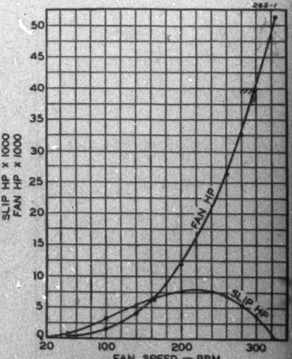


Figure 1. Speed-horsepower chart of a propeller-type fan, with slip energy for wound-rotor induction-motor drive

Synchronous speed of induction motors 327 rpm
Maximum speed of fan 300 rpm
Rated load 40,000 horsepower at 300 rpm

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amount of the magnetizing current. Figure 10 shows the loci for $S=0.96$ plotted to the same scale.

(o) By a procedure paralleling that outlined above in items (d)–(l), inclusive, any desired locus can be found. Both conditions of item (k) must be satisfied for I_{2b} as well as for I_{2a}.

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1. The Cross-Field Theory of the Capacitor Motor, A. F. Puchstein and T. C. Lloyd. AIEE Transactions, volume 60, 1941, February section, pages 58–61.

2. The Revolving-Field Theory of the Capacitor Motor, W. J. Morrill. AIEE Transactions, volume 48, 1929, pages 614–629.

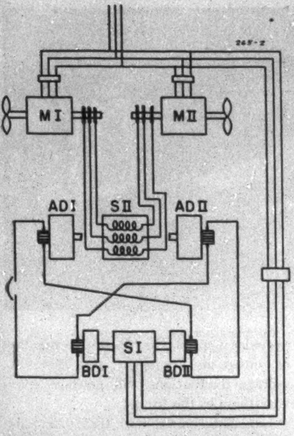

Figure 2. Schematic connection diagram with double motor drive using counterrevolving propeller

MI and MII = 22,000-horsepower slip-ring 327-rpm fan motors
ADI and ADII = 2,500-kw d-c generators
BDI and BDII = 3,200-horsepower d-c motors
SII = 20,000-kva 540-rpm synchronous motor
SI = 6,000-kva 514-rpm synchronous generator

factory as a cooling medium. It is, therefore, necessary to remove the heat from the nacelle by other means. Air-to-water heat exchangers could be used for this purpose, the coolers being so arranged that blowers located in the nacelle draw the hot air through the cooler, where it gives up its heat, delivering the cool air back to the motor. Water for the coolers is obtained from the sump of a cooling tower. The use of the coolers will reduce the ambient appreciably over the tunnel ambient, thus permitting higher temperature rise with consequently smaller and less expensive equipment.

Referring again to Figure 2, induction motors operated in this manner are doubly fed. The speed at any instance is proportional to the difference between the stator and rotor frequency. The machines, therefore, operate without slip in the sense that that term is usually applied, and, in effect, operate substantially as a synchronous machine. In fact, if we arrange to compensate for the *IR* drop in the machine windings, they can be made to operate exactly in synchronism.

Speed variation is accomplished by varying the fields of the d-c machines used in the circuit. This provides the equivalent of full adjustable-voltage or generator field control, and this control is accomplished through the use of d-c machines having approximately one-sixth the capacity of the total power absorbed by the propellers.

Accuracy of speed control is obtained through the use of high-speed excitation on the d-c machines. The excitation is controlled by matching a tachometer voltage against a standard, amplifying the difference that exists and applying this difference to the field of a high-speed exciter (Amplidyne generator).

The voltage standard is a 250-volt excitation bus whose voltage is maintained within $^1/_{10}$ of a volt. Speed variation is obtained by positioning a potentiometer rheostat having approximately 800 positions for the speed range of 50 to 300 rpm. This rheostat need be no more formidable than the rheostat used to control the tuning of a radio. However, since the problem of controlling this much power extends back to the central station serving the project, it is necessary to impose restrictions on the operators in order not to build up the load too rapidly, on the one hand, lest the power system be unnecessarily disturbed, and on the other, to prevent operators whose attention might be engrossed at the moment from blowing low-speed models to pieces in a high-speed tunnel.

Power-factor correction is obtained by varying the excitation on the variable-speed synchronous machine connected across the slip rings. In order to relieve the operator of the responsibility of holding the proper power factor, amplidyne control was applied to the field of the synchronous machines and adjusted to provide a power factor of 90 per cent at all loads and speeds. This is quite an important feature, since induction motors ordinarily applied to an operation of this kind would draw approximately 10,000 kva lagging at the lower speed range where a large percentage of the operation will take place.

Low-starting kilovolt-amperes is obtained by starting the constant-speed motor-generator set. After this set is started, the variable-speed set is brought up to provide 60 cycles, and with proper excitation applied at the rotor, it is possible to match the line voltage and frequency very accurately on the stator of the induction machine. When the voltage and frequency are matched, the running breakers may, of course, be closed. The voltage and frequency of the variable-speed synchronous machine are controlled through automatic synchronizing devices. Through this system, a 40,000 horsepower drive may be started with an inrush under 6,000 kva.

The rate of power change is controlled by using a motor-driven potentiometer rheostat. This rheostat may, of course, be driven at any desired speed. The rheostat may, if desired, be tapered so as to provide a high rate of change over the lower speed range of the drive with corresponding low rate of change at the higher speed range and, consequently, higher power demand point. On the other hand, it may not be desirable to change the speed of the machine too quickly at any part of the range, as this might make it difficult to obtain accurate data. It is conceivable that it might be desirable to change the speed very slowly in order to observe the effect of this speed change on the model. Suffice it is to say that practically any type of speed-versus-time characteristic could be obtained very easily within the design limitations of the associated apparatus.

Concerning the desirable though not necessarily mandatory characteristics, it is well to mention that the load factor on an operation of this kind is low, perhaps not more than 10 per cent. Power consumption will, therefore, be the least expense associated with the operation of the tunnel, and the economics of the problem do not justify any appreciable lay-

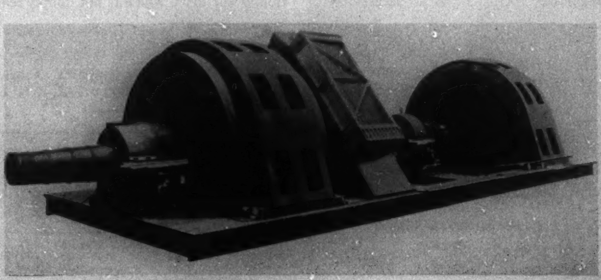

Figure 3. Complete assembly of two 8,700-horsepower 327-rpm wound-rotor induction motors and surface air coolers for wind-tunnel drive

Motors are not coupled together

Figure 4. Close-up view of collector end of one 8,700 horsepower 327-rpm wound-rotor induction motor, of assembly shown in Figure 3

Figure 5. Variable-speed motor-generator set for speed adjustment of two 15,000-horsepower 327-rpm wound-rotor induction motors on wind-tunnel drive

A-c synchronous machine rated 13,500 kva, 0.9 power factor, each d-c machine rated 1,750 kw

out for increased efficiency. It is well to point out in this connection, however, that while it is necessary to pay for this energy on the one hand, the expense of disposing of it may more than equal its cost in the first place. The maximum demand on such an installation is often of greater importance than the power consumed. Therefore, the higher the efficiency, the lower will be the demand, and at the maximum loss point which occurs around 70 per cent speed, the maximum demand may be reduced 27 per cent over another system which would waste this slip energy.

The efficiency of the system described here is necessarily high since all slip energy is returned to the line minus machine losses. In the case of slip-regulator operation, it is necessary to operate with a higher slip than is provided by an induction motor operating with a shorted secondary. The electrodes of slip regulators can not be brought in too close proximity, otherwise erratic operation is obtained. Therefore, the full load efficiency of the two systems is comparable. At any reduced operating-speed point, however, the efficiency of the doubly fed motor will be much higher than the efficiency of any system where the slip energy is dissipated.

Ease of speed control is, of course, a by-product of the system used. Speed with this system is a function of frequency rather than voltage, and frequencies of present day distribution systems are maintained with a high degree of accuracy. Temperature changes are ineffectual except in the loop circuit of the d-c machine. Changes here are very gradual and are easily taken care of by the regulating equipment. Any type of induction-motor drive which is not doubly fed provides a speed which is both a function of voltage and frequency, and, therefore, voltage fluctuations will produce speed variations in the tunnel.

Line disturbances with this system are, of course, reduced to an absolute minimum, since there is no high-voltage switching under power, and after the machines are once started, the only change is a gradual build-up or decay of the load. Since the rate of change of this load may be very accurately and completely controlled, the generating system will be enabled to pick up and drop the load at a uniform rate, permitting the distribution system regulating devices to function and maintain normal service.

Low maintenance should result where only the rotating apparatus is employed and where there is no switching of dynamic current. In fact, the entire control operation is performed by handling current in the order of a fraction of an ampere. Maintenance is always appreciable where dynamic currents are switched regardless of the means provided to accomplish a change in operating conditions.

The system is, of course, new, and it is particularly adapted to the operation of fan loads, centrifugal pumps, frequency-converter systems, where the two frequencies do not necessarily match exactly, and where it is desired to accurately control the flow of power.

The Influence of Towers and Conductor Sag on Transmission-Line Shielding

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THIS paper is the third of a sequence of papers intended to present data which may be used in determining the degree of protection from lightning obtainable by shielding transmission lines and structures with grounded overhead wires and masts. The first two papers of the sequence are: "Shielding of Transmission Lines,"[1] and "Shielding of Substations."[2]

Results Based on Earlier Model Tests

One sentence in the synopsis of a paper, "Lightning Protection for Oil Storage Tanks and Reservoirs,"[3] presented at the 1927 Pacific Coast convention of the AIEE, reads as follows:

> "Tests show that excellent protection can be obtained by towers properly installed, but they do not indicate absolute immunity against hits."

The tests described in that sentence were laboratory tests on small-scale models. Using data obtained from those and other tests, a plan for protecting reservoirs by means of masts was developed. The integrity of those tests in indicating the protective value of shielding by grounded masts is well demonstrated. All oil reservoirs equipped with masts designed according to the data obtained from the tests described in that paper have been free from damage by lightning since the masts were erected during 1926 and 1927. In some installations, well-grounded masts only were used, and in others, where conditions indicated it advisable, the masts were supplemented by interconnecting overhead conductors.

In the 14 years which have elapsed since that method of protection was adopted for the oil reservoirs in question, many field data and many model tests have added to our knowledge of lightning and ways to guard against lightning damage. A bibliography of the reported work relating to this particular discussion was presented in the paper, "Shielding of Transmission Lines." One dictate of the knowledge obtained is that lightning protection for electrical transmission lines and other structures can be obtained most economically in any scheme of protection by taking advantage of the shielding effect of overhead ground wires and grounded masts located in proper juxtaposition to the objects to be protected. Since absolute protection for such structures is not usually economically practical, engineers desire statistical data which will enable them to determine the degree of protection provided by particular arrangements of overhead ground wires and masts. Such data must be obtained by many observations of actual lightning strokes and by careful model tests in the laboratory. The model tests must show valid correlation with field observations, and duplicate those characteristics of natural lightning which determine the paths taken by strokes. A full discussion of this topic appears in the first paper of the sequence.

Recent Model Tests

The first paper presented data showing the value of ground wire protection for a laboratory model representing a section of transmission line with a tightly-stretched conductor protected by a parallel tightly-stretched overhead ground wire. There was no appreciable sag in the section of model line tested, and no supporting towers were included within the test area.

The present paper gives the results of two check tests made in the California Institute of Technology high-voltage laboratory upon models identical with those used for two of the tests made at Trafford, and of further tests which determine the shielding effect, additional to that of overhead ground wires, provided by the transmission towers and conductor sag. Correction factors are given by means of which shielding data for parallel wires can be modified to account for the additional protection resulting from the presence of transmission towers.

Table I. Distribution of Stroke Terminations to a Typical 1,000-Foot Transmission-Line Span (Per Cent)

Model Arrangement	Conductor	Ground Wire	Tower	Ground Plane
(*a*) Taut wires only	23.4	58.5		18.1
(*b*) Tower, taut wires	18.7	50.1	12.7	18.5
(*c*) Tower, sagged wires	8.9	44.6	15.8	30.6

The large amount of published data relating to lightning, surge testing with models, and the correlation between field observations and model tests under various conditions, permits the writing of this paper without including any matter relating to the "mechanism of natural lightning," and with little reference to the "fundamentals of model tests."

In keeping with conclusions of other experimenters, and the experience of the authors, it was considered justifiable for these tests to duplicate as closely as possible the test conditions used in obtaining the data for the first paper.

Laboratory Model and Test Conditions

The 2,000,000-volt 0.065-microfarad surge generator, built by graduate students at Pasadena, was used as the voltage source for the work of this report.

The conventional surge-generator circuit, with a resistance in parallel with the

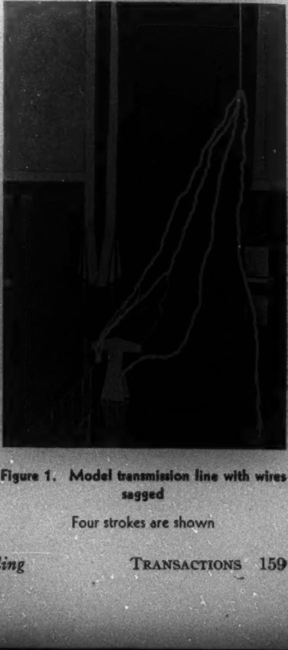

Figure 1. Model transmission line with wires sagged

Four strokes are shown

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The authors acknowledge the assistance of E. A. Bureau, University of Kentucky, the several graduate students who assisted in making observations, and the National Youth Administration.

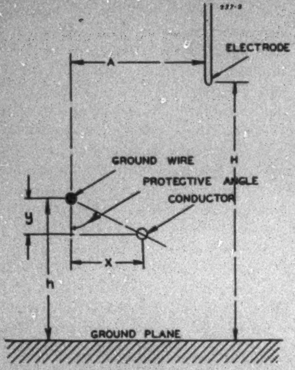

Figure 2. Taut ground wire and conductor parallel

Used to obtain data for Figures 3 and 4

test gap, was used. The wave form was approximately a $1^1/_2$- by 40-wave; and all strokes were fired at the minimum arc-over voltage of the test gap, with cloud electrode polarity positive. The discharge electrode was a $^3/_8$-inch diameter rod with a rounded end, mounted vertically with its lower end 50 inches above the ground plane of the model. The ground plane was a large salt water basin in which was submerged a grid of ground wires, covering the entire area of the basin and providing a 12-inch mesh over the test area. Conductors and tower used in the model were solidly grounded.

The model represented 100-foot transmission towers supporting 1,000-foot spans, built to a scale of 10 inches = 100 feet (see Figure 1). Only one mode lscale was used, because experience has demonstrated that change in model scale does not change the relations of test results.[1] For test purposes, number 14 bare copper wire was used for overhead ground wire and conductor. For tests with sagged wires, the proper catenary was maintained by use of nonconducting anchor cords kept dry by having their lower ends attached to metal hooks just above the water.

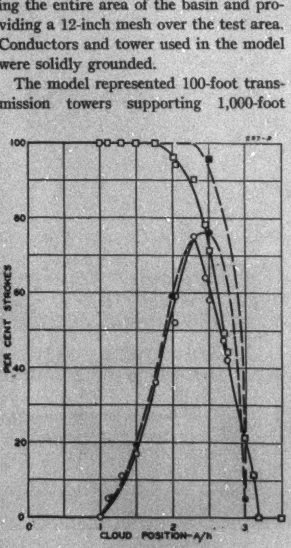

Figure 3. Check tests

y/h = 0.1. Solid curves Pasadena test data. Dotted curves Trafford data

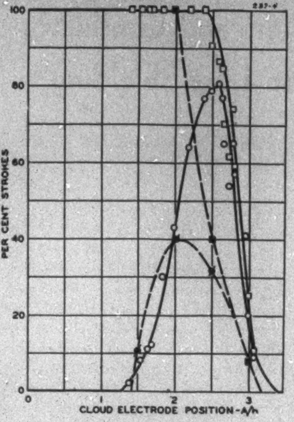

Figure 4. Check tests

y/h = 0.2. Solid curves Pasadena test data. Dotted curves Trafford data

To the scale of 10 inches = 100 feet, the equivalent height of the cloud electrode was 500 feet. This is the generally recognized minimum height of cloud from which lightning develops. This arrangement gives conservative results for shielding conditions. The first paper included a study of the effect of protective angle between ground wire and conductor. One set of data was for an angle of 64 degrees. In that test, a measurable percentage of strokes terminated on the "protected line conductor." This 64-degree angle was chosen as a fixed reference angle and used throughout the tests reported in this paper.

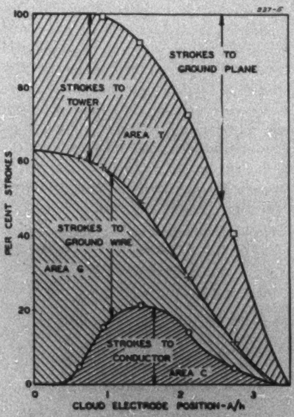

Figure 5. Explanation of distribution curves

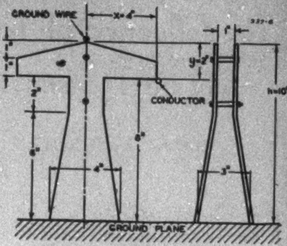

Figure 6. Detail of model tower

Scale 10 inches = 100 feet

Methods of Observation

In reporting the tests, the total number of strokes for any condition was divided into groups and classified according to the points of stroke termination. The strokes were designated as strokes to conductor, strokes to overhead ground wire, strokes to tower, and strokes to ground plane. At least 100 strokes were "fired" for each position of the cloud electrode. The points of stroke termination were recorded by two observers viewing the model from positions such as to have their lines of vision intersect perpendicularly at the point subject to test. This made it possible to determine accurately and without any difficulty the point of stroke termination. The observers exchanged positions after each 25 strokes. If results of the first 50 strokes were not consistent with those of the following 50 strokes, additional strokes were fired. The lack of dependence of results upon any unique skill or opinion of an observer is well established by the agreement in the reports of the 16 observers used during the series of tests.

Model Arrangements

Tests were made under three conditions:

1. As shown in Figure 2, using taut ground wire and conductor parallel to each other, without tower in place or conductor sagged. These were check tests to correlate this work with that reported in the first paper.

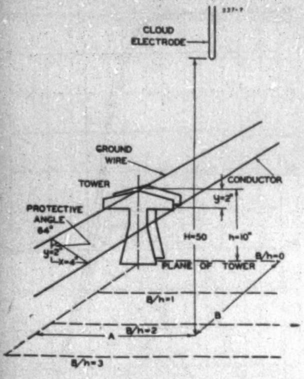

Figure 7. Taut ground wire and conductor parallel

Tower in place. For Figures 8–12, 22, and 24

2. As shown in Figure 7, using taut ground wire and conductor with model tower installed to determine added shielding effect of tower.

3. As shown in Figure 13, using ground wire and conductor under less tension and suspended from model tower to simulate the sag that is always present for actual transmission-line conditions. Tests were made with this arrangement to evaluate the effect of line sag on shielding.

Results of Check Tests

In the check tests, the wires representing the line conductor and the overhead ground wire were tightly-stretched copper wires supported outside the test area and without towers or line sag within the test area. The first check test was made with $h=10.0$ inches; $y/h=0.10$; $H/h=5$; protective angle 64 degrees (see Figure 2); and the second check test was made with the same arrangement except for a change in the ratio y/h from 0.10 to 0.20. Figures 3 and 4 show the comparative results of the tests made in the two laboratories. The Pasadena test results are plotted as solid-line test curves, and the Trafford test results as dotted-line test curves.

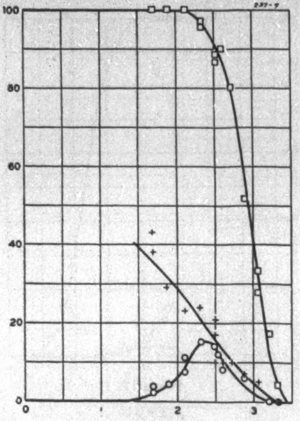

Figure 9

$B/h=0.5$

The results of the two check tests, while not in exact agreement with the Trafford tests, are as much in accord as one could expect for tests of this character made in different laboratories by different experimenters who have no agreement as to number of "shots" for each condition, and other plans of procedure.

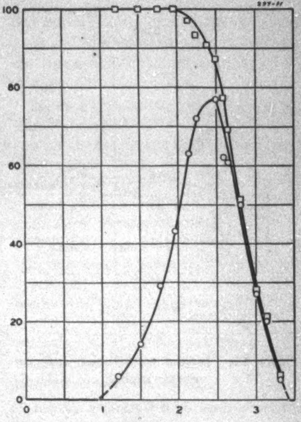

Figure 11

$B/h=2.0$

The differences which are to be noted in the curves may be due to the difference in the number of strokes made to each point. The solid curves represent more than 1,500 strokes for each curve and a minimum of 100 strokes for each A/h value used in plotting the curve. However, in considering actual lines, the effect of line sag and towers is such as to more than make up for the difference in the two

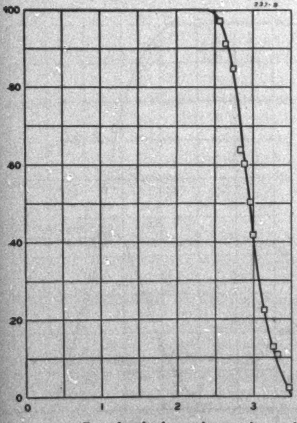

Figure 8. For cloud electrode in plane of tower

$B/h=0$

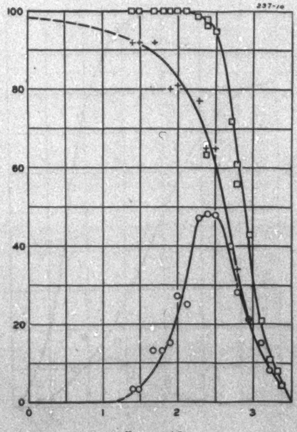

Figure 10

$B/h=1.0$

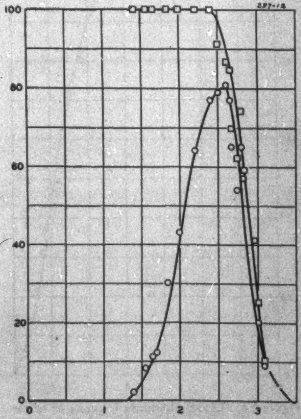

Figure 12. Tower removed

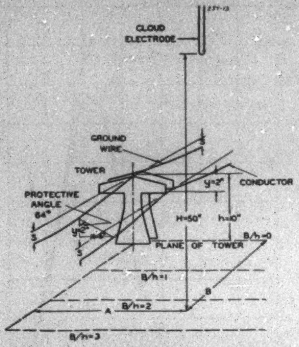

Figure 13. Sagged ground wire and conductor parallel

Tower in place. For Figures 14–20 and 23

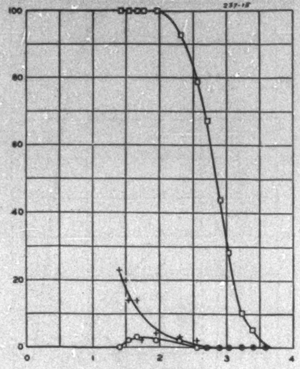

Figure 15

$B/h = 0.5$

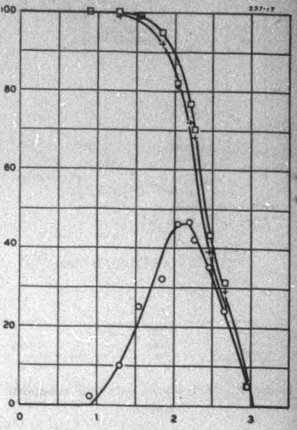

Figure 17

$B/h = 2.0$

curves. One result of the comparison just made is the indication that it is well in making surge tests of the character under discussion to base findings on results obtained by making at least 100 "strokes" for each plotted point. See Table II.

Shielding Effect of Towers

The data obtained with towers in the model line added to our general realization of their value as aids in line shielding by providing data which show that, for a cloud electrode at five times tower height above ground, a line conductor suspended below a metallic cross arm is completely protected from strokes originating from a cloud point in a plane through the tower and perpendicular to the line. The data also show that the protection furnished by the tower decreases with distance from the tower measured along the line, as follows:

> With 100 per cent protection at the tower, there is only 85 per cent protection with the cloud point moved parallel to the line a distance of one-half tower height from the tower. If the distance from the plane of the tower to the point of cloud discharge is equal to a full tower height, the protection is only 50 per cent. The towers offered no protection to the line conductor against strokes originating from a cloud point more than two times tower height along the line from the plane of the tower (see Figure 22). Towers 100 feet high and spaced 1,000 feet apart in a transmission line reduce the number of probable strokes to the conductor for the over-all line to 80 per cent of the number that would be expected for a section of line without towers. The line conductor and overhead ground wire were kept taut without appreciable sag in these tests (see Figure 7).

Shielding Effect of Line Sag

The third set of tests on the model line was made with the tower in place and with line conductor and overhead ground wire kept parallel to each other, both being sagged 40 per cent of the tower height at the center of the span (see Figure 13). These tests show that with 100-foot towers 1,000 feet apart, the number of strokes that hit the conductor for the cloud

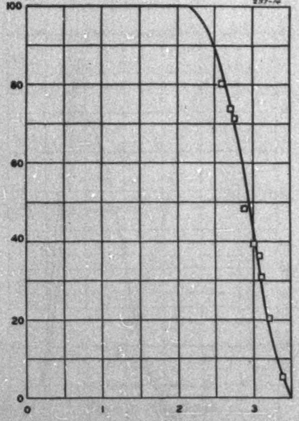

Figure 14. Cloud electrode in plane of tower

$B/h = 0$

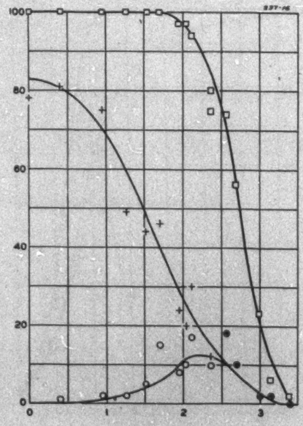

Figure 16

$B/h = 1.0$

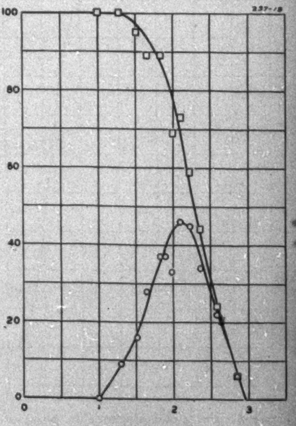

Figure 18

$B/h = 3.0$

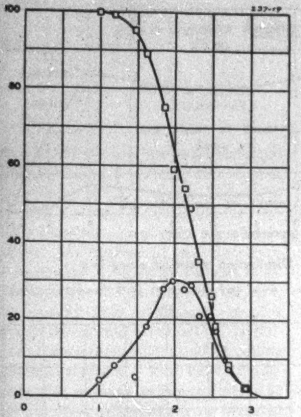

Figure 19

$B/h = 4.0$

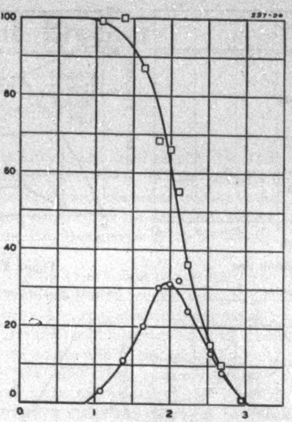

Figure 20

$B/h = 5.0$

electrode opposite the center of the span was only 38 per cent of the number observed when the lines were not sagged. For a uniform distribution of strokes originating from clouds along the line, the line sag reduced the number of strokes to conductor to 52 per cent of the number obtained when towers were in place but there was no line sag (see Figure 23).

Distribution of Strokes

Tests involving thousands of strokes to the model-line area from a minimum cloud height of five times the tower height showed that the line is immune to strokes that originate along the line, outside a band, the width of which is about seven times the tower height. Table I presents the distribution for strokes originating from the cloud electrode located above the line at points uniformly distributed within a band having a width of seven tower heights and with its axis parallel to the axis of the transmission line.

Explanation of Figures

Figures 2 to 24, showing model arrangement and results of tests, enable ready comparison of results. In these diagrams:

h = Height of tower and ground wire at point of tower support. In tests this model was always 10 inches high.

y = Vertical component of displacement of conductor below ground wire.

x = Horizontal component of displacement of conductor from ground wire.

$\underline{/P}$ = Protective angle $= \tan^{-1} x/y$.

H = Height of lower end of cloud electrode above ground plane = 50 inches.

A = Horizontal component of displacement of cloud electrode *normal* to the line, measured from ground wire.

B = Horizontal component of displacement of cloud electrode *parallel* to the line, measured from center line of tower.

s = Sag of ground wire and of conductor at center of span, from heights at tower support points.

For each of the three tests, all model dimensions were fixed, except the horizontal displacement of the cloud electrode, given by A and B, which was varied throughout the region from which strokes might terminate on the model line.

Figure 5 explains the curves of test results. For each value of A/h, the percentage strokes to conductor are shown by the ordinate to the lowest curve, test points shown by ⊙; the percentage to ground wire by the difference between the ordinate of the curves whose points are (+) and the curve whose points are ⊙; the percentage strokes to tower by the differences between the curves whose points are (□) and the curve whose points are (+). Strokes to ground plane are represented by the difference between the 100 per cent ordinate and the curve whose ordinates are marked by (□).

Figure 2, shows arrangement of model line with taut wires only.

Figure 3 shows results for $y/h = 0.1$, and Figure 4 results for $y/h = 0.2$ for tests at both laboratories, with wires only as shown in Figure 2. Figures 5 and 6 are the explanatory curve and detail of model tower, respectively.

Figure 7, shows arrangement of model line with tower in place and taut wires.

Figure 8 shows stroke distribution for electrode at various positions (A/h) in the plane of tower ($B/h = 0$).

Figure 9 shows stroke distribution for electrode at various positions (A/h) in the

Table II. Typical Test Observation Data

	Test 89—Tower, Taut Wires 5 Strokes in Each Group y/h = 0.2; B/h = 1.0; A/h = 2.40					Test 149—Tower, Sagged Wires 5 Strokes in Each Group y/h = 0.2; B/h = 1.0; A/h = 2.36			
	Stroke Distribution					Stroke Distribution			
	Tower	Ground Wire	Conductor	Ground Plane		Tower	Ground Wire	Conductor	Ground Plane
	1	3	1	0		4	0	0	1
	2	1	2	0		3	0	0	2
	3	1	1	0		3	0	0	2
	0	0	5	0		4	0	0	1
	1	1	3	0		5	0	0	0
	2	0	3	0		2	0	1	2
	1	0	4	0		3	0	1	1
	1	2	2	0		3	0	1	1
	0	2	3	0		3	0	2	0
	3	0	1	1		2	0	2	1
First 50 Strokes	28%	20%	50%	2%	First 50 Strokes	64%	0%	14%	22%
	4	0	1	0		3	0	2	0
	1	1	3	0		2	1	1	1
	2	1	2	0		3	0	0	2
	2	1	2	0		4	0	0	1
	1	2	2	0		3	0	1	1
	1	0	4	0		1	0	2	2
	1	1	2	1		4	0	1	0
	2	0	3	0		2	0	1	2
	3	0	1	1		5	0	0	0
	1	1	3	0		4	0	1	0
Second 50 Strokes	36%	14%	46%	4%	Second 50 Strokes	62%	2%	18%	18%
100 Strokes	32%	17%	48%	3%	100 Strokes	63%	1%	16%	20%

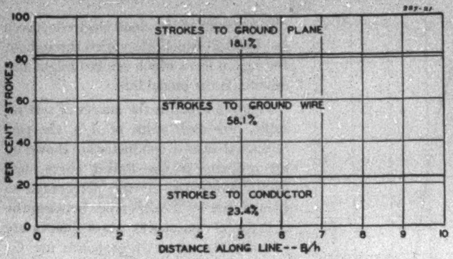

Figure 21. Distribution of strokes along line

Taut parallel ground wire and conductor only

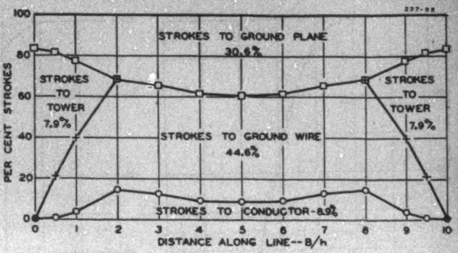

Figure 23. Distribution of strokes along line

Sagged parallel ground wire and conductor with towers in place

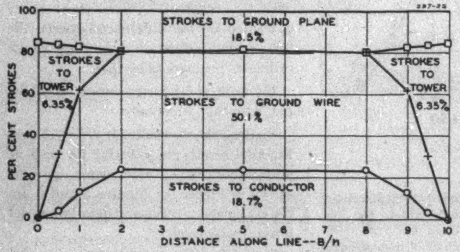

Figure 22. Distribution of strokes along line

Taut parallel ground wire and conductor with towers in place

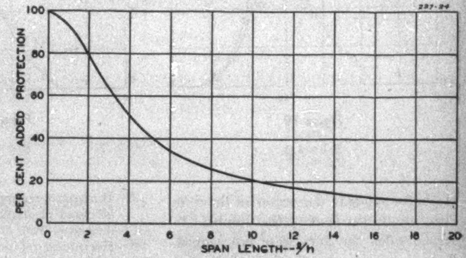

Figure 24. Added protection resulting from presence of towers, for various lengths of span

plane one-half tower height away from tower ($B/h=0.5$).

Figures 10, 11, and 12, show stroke distribution for $B/h=1.0$; for $B/h=2.0$; and with tower removed, respectively.

Figure 13, shows arrangement of model line with tower in place and 40 per cent sag in wires. Figures 14 through 20 show stroke distribution for electrode at various positions A/h in planes for values $B/h=0.0$; 0.5; 1.0; 2.0; 3.0; 4.0; and 5.0.

Interpretation of Test Data

The areas C, G, and T, bounded by the curves, as illustrated in Figure 5, show values of stroke distribution to conductor, to ground wire, and to tower, integrated for all values of A/h. The remaining area shows strokes to ground plane for integrated values of A/h up to about 3.5 times tower height.

Figures 21, 22, and 23, in columns 1, 2, and 3, respectively, show the distribution of strokes along the line, where B/h measures distance from tower in terms of tower height (h). The ordinates of these curves for each (B/h) value were obtained from the areas of the preceding curves. The areas of Figures 21, 22, and 23 show the distribution for stroke terminations for an entire span. The data for Table I were obtained by measuring these areas.

Figure 24, derived from data for Figure 22, shows as a function of span length the percentage of added shielding provided by towers. For a 40 per cent sag in a 1,000-foot span, a similar curve could be drawn showing the added shielding effect of towers and sag. This has not been done because in actual practice sag changes with tower spacing and conductor tension.

Conclusions

The tests on which this report is based show that the Wagner, McCann, and MacLane protection values are conservative, because they do not include the protection provided by towers and line sag. The preceding curves can be used to determine the additional protection due to towers and conductor sag for a protective angle of 64 degrees. For other protective angles estimates of the added protection can be based on reasonable interpretation of these results. The magnitude of this added protection is important, since for typical spans it shows an increased protection of 20 to 80 per cent.

Appendix

Figure 25 is included in this paper to call attention to the pattern formed in the grounding pool, apparently on the surface of the water. At the time this picture was made, the water had a somewhat higher resistance than was used during the regular testing program. Figure 1 was made after the addition of more salt to the water. The "crowfoot" figures were then less noticeable. Figure 8 in the paper, "Lightning Protection for Oil Storage Tanks and Reservoirs"[3] showed the spread of discharge current over a concrete floor. The "crowfoot" travel of current over that floor made a photograph much like the ones shown in this paper. These pictures are of interest in showing what may happen when large currents arrive at a point of sudden change in resistance. The resistance of the concrete floor was very high.

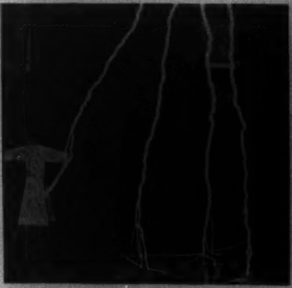

Figure 25. "Crowfooting" or spreading of streamers on surface of abnormally high-resistance water plane

A Fast Circuit Breaker

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MEMBER AIEE

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DURING the past few years the rapid expansion of mercury-arc rectifier installations at 600 volts d-c, both as to the total kilowatts installed and the relatively large number of units operated in parallel, has emphasized the importance of switchgear in providing suitable rectifier operation. In the event of a backfire, the rates of current rise through a rectifier and its transformer windings lie, in general, between three million and six million amperes per second. With large installations, such as shown in Figure 1 where 60 units of 5,000 amperes each are operated in parallel, the ceiling value of these currents is far above a figure which could be tolerated both from the standpoint of continuity of operation and safety to equipment.

High-speed breakers having a time of approximately 0.5 cycle from backfire initiation to current limitation may permit, in some installations, peak currents in the neighborhood of 60,000 amperes. While such a value is appreciably below that which would cause equipment damage, it is nevertheless undesirably high, in that surges and "sympathetic" backfires on other units often result. Breaker duty and maintenance are higher than would be the case with lower values of backfire current, and the factor of safety is not so great as is desirable.

Figure 2 shows the current path during a backfire in a six-phase rectifier. It will be noted that the current flowing through the cathode breaker is supplied by other rectifiers on the bus, and that the current flowing through the anode breaker is the same current plus the additional current from the other anodes of the faulted rectifier.

Figures 3 and 4 show two accepted connection schemes for rectifier stations. In Figure 3 a high-speed cathode breaker opens the backfire current fed from other units on the bus of the backfiring unit. At some later period, such as six or eight cycles, the rectifier-transformer primary oil switch opens and clears the backfire current supplied by anodes in the backfiring unit. Since an oil circuit breaker ahead of the transformer may be called upon to interrupt the full capacity of the system, its economical disadvantage is obvious. Furthermore, its slow clearance of a transformer secondary short circuit, caused by a backfire, is undesirable.

Figure 4 shows an alternative connection scheme in which the individual trip-free poles of a six-pole anode breaker clear backfire currents fed from the other rectifier operating in parallel with the backfiring unit, and also interrupt the backfire current supplied by the anodes in the unit. The semihigh-speed cathode breaker is used for backup protection and to provide, through gang tripping, means of dropping a large unit of load fed from a group of rectifiers.

This paper discusses a six-pole anode breaker primarily designed to provide reduced operating time on reverse current and thus to cut to a minimum the undesirable effects of backfires. The same principles of design incorporated in this circuit breaker could apply equally well to a high-speed cathode breaker.

High-speed breakers may be of either the latched-in or nonlatched-in type. If of the former design, the latch-releasing mechanism may be in the form of a simple trip, presumably designed with a maximum force-to-weight plunger ratio when energized with reverse current, or it may have some form of bucking-bar latch-releasing mechanism, or the latch may consist of a friction clutch. With all of these types of breaker, a relatively large

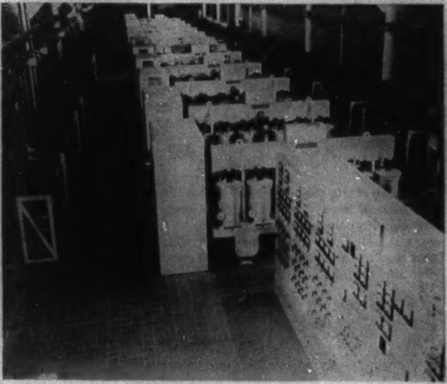

Figure 1. View of large rectifier installation

Paper 42-38, recommended by the AIEE committee on protective devices for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 3, 1941; made available for printing December 12, 1941.

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The discharge currents from the arc path into the water forming the ground plane for Figures 1 and 25 were estimated to be of the order of 10,000 amperes. No effort was made at the time of the experiments to measure the resistance of the ground circuit including the arc during the stroke. To get some idea of the probable resistance of the arc path to ground, a brass rod was substituted for the arc path. With the end of the rod dipped one inch into the water, the ground circuit resistance was found with an ohmmeter to vary from 390 ohms to 250 ohms, depending upon the amount of salt added to the water. With one-half inch of electrode immersed in the water, the resistance was 800 ohms. In the tests made with the tower in place, the resistance of tower to ground was three ohms.

References

1. Shielding of Transmission Lines, C. F. Wagner, G. D. McCann, and G. L. MacLane, Jr. AIEE Transactions, volume 60, 1941, pages 313–28.

2. Shielding of Substations, C. F. Wagner, G. D. McCann, and C. M. Lear. AIEE Transactions, volume 61, 1942, February section, pages 96–100.

3. Lightning Protection for Oil-Storage Tanks and Reservoirs, Royal W. Sorensen, James Hugh Hamilton, and Claude D. Hayward. AIEE Transactions, volume 47, 1928, pages 164–80.

4. The reader will find bibliographies in reference 1 and in the following series of three articles: Lightning Phenomena, C. F. Wagner and G. D. McCann. Electrical Engineering, volume 60, 1941. (a) I—General Characteristics, August, pages 374–84. (b) II—Instruments for the Measurement of Lightning Surges, September, pages 438–43. (c) III—Field Studies, October, pages 483–500.

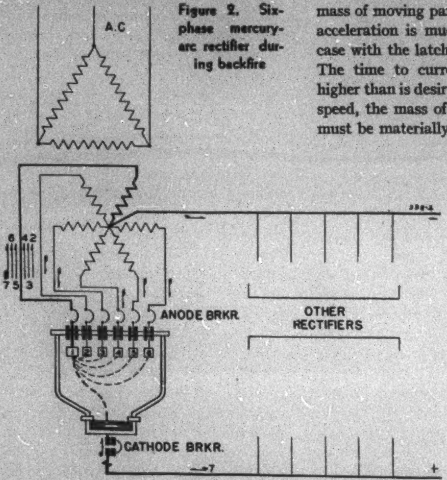
A.C
6 4 2
7 5 3
ANODE BRKR.
OTHER RECTIFIERS
CATHODE BRKR.
238-2
7
+

Figure 2. Six-phase mercury-arc rectifier during backfire

proportion of the total operating time is absorbed in operating the releasing mechanism.

For example, a predecessor of the breaker herein discussed has a polarized solenoid-trip mechanism whose plunger releases a latch engaging a ball-bearing-mounted latch pin on the main arm. This breaker trips on about 1,500 amperes reverse current and about 6,000 amperes forward current, the polarizing providing a bias. A typical time study of this circuit breaker, based on oscillograms simulating backfires, shows the following:

Completion of trip plunger stroke and release of latch	0.17 cycle
Total time to parting of arcing contacts	0.38 cycle
Total time to current peak	0.50 cycle
Total time to end of arcing	0.7 cycle

It will be observed that of these various times the first value, 0.17 cycle, is completely wasted in that the breaker mechanism proper is not released until the expiration of this time. This indicates that, even if the succeeding times were appreciably reduced, no great reduction in the total time to current peak would be obtained.

This initial time can be reduced to nearly zero by using a bucking-bar holding magnet with the armature fastened directly to the main operating arm. With this design, the main contact arm starts to move in 0.03 or 0.04 cycle (60-cycle basis) after the backfire is initiated.

Unfortunately, the difference between these latter figures and the previous figure of 0.17 cycle is not a net gain, as the total mass of moving parts is so great that the acceleration is much slower than is the case with the latched-in type of breaker. The time to current limitation is still higher than is desirable. In order to gain speed, the mass of the holding armature must be materially reduced as compared with the conventional type of holding magnet armature.

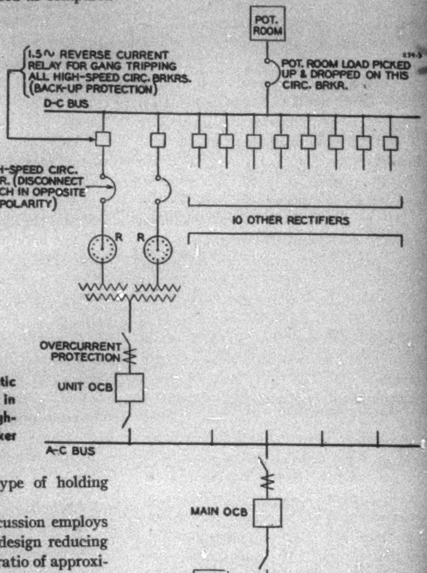
POT. ROOM
1.5∿ REVERSE CURRENT RELAY FOR GANG TRIPPING ALL HIGH-SPEED CIRC. BRKRS. (BACK-UP PROTECTION)
238-3
POT. ROOM LOAD PICKED UP & DROPPED ON THIS CIRC. BRKR.
D-C BUS
HIGH-SPEED CIRC. BRKR. (DISCONNECT SWITCH IN OPPOSITE POLARITY)
10 OTHER RECTIFIERS
R
R
OVERCURRENT PROTECTION
UNIT OCB
A-C BUS
MAIN OCB

Figure 3 (right). Schematic diagram of main connection in rectifier station, using high-speed cathode circuit breaker

The breaker under discussion employs a magnet and armature design reducing the armature weight in a ratio of approximately 1 : 3, as contrasted with a conventional holding magnet.

Figure 5 illustrates a holding magnet, the bucking-bar feature being omitted. For illustration, this magnet is assumed to have an effective armature area of 5.25 square inches. Figure 6 shows a design in which the proportions have been greatly altered, although the armature area and hence its pull are the same as shown in Figure 5.

It will be observed that the design in Figure 6 shows a weight reduction of

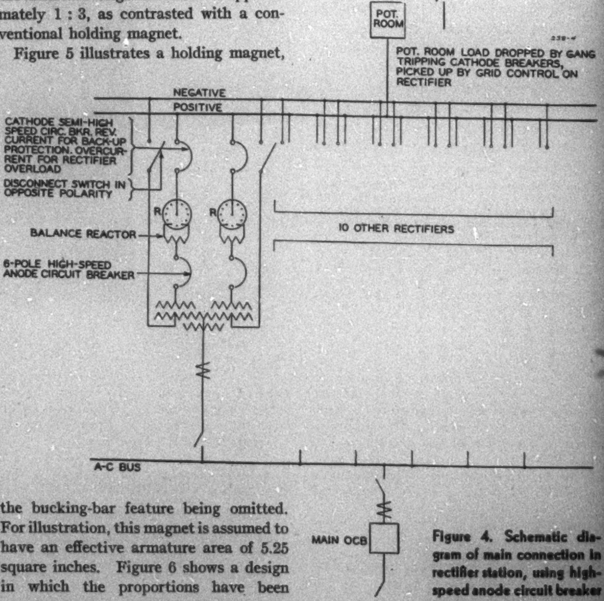
POT. ROOM
238-4
POT. ROOM LOAD DROPPED BY GANG TRIPPING CATHODE BREAKERS, PICKED UP BY GRID CONTROL ON RECTIFIER
NEGATIVE
POSITIVE
CATHODE SEMI-HIGH SPEED CIRC. BKR. REV. CURRENT FOR BACK-UP PROTECTION. OVERCURRENT FOR RECTIFIER OVERLOAD
DISCONNECT SWITCH IN OPPOSITE POLARITY
R
R
BALANCE REACTOR
10 OTHER RECTIFIERS
6-POLE HIGH-SPEED ANODE CIRCUIT BREAKER
A-C BUS
MAIN OCB

Figure 4. Schematic diagram of main connection in rectifier station, using high-speed anode circuit breaker

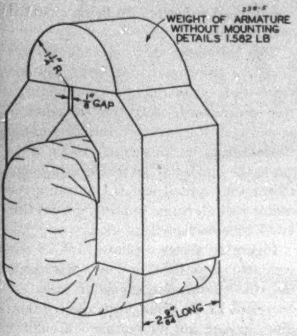
238-5
WEIGHT OF ARMATURE WITHOUT MOUNTING DETAILS 1.582 LB
1 1/4" R
1/8" GAP
2 9/64" LONG

Figure 5. Conventional design of holding magnet

about 3:1, as compared with that in Figure 5, but quite obviously such a design is entirely impractical from a mechanical and stiffness standpoint, so its theoretical advantages cannot be realized.

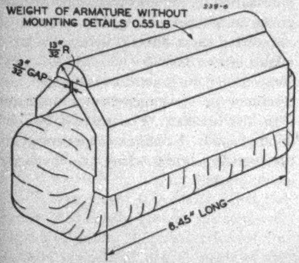
238-6
WEIGHT OF ARMATURE WITHOUT MOUNTING DETAILS 0.55 LB
13/32" R
3/32" GAP
6.45" LONG

Figure 6. Elongated design of holding magnet

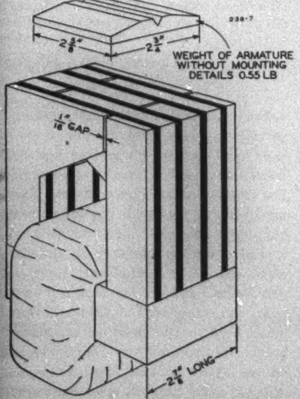
238-7
2 5/8"
2 3/4"
WEIGHT OF ARMATURE WITHOUT MOUNTING DETAILS 0.55 LB
1/16" GAP
2 7/8" LONG

Figure 7. Design of holding magnet used in one-quarter-cycle breaker

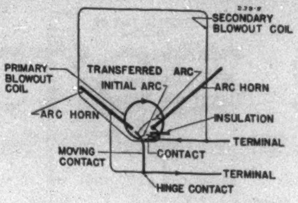
238-8
SECONDARY BLOWOUT COIL
PRIMARY BLOWOUT COIL
TRANSFERRED ARC
INITIAL ARC
ARC HORN
ARC HORN
INSULATION
TERMINAL
MOVING CONTACT
CONTACT
TERMINAL
HINGE CONTACT

Figure 8. Schematic diagram of circuit-breaker magnetic-blowout structure

Figure 7 shows the design employed in the breaker under discussion. It carries out the same general principle of weight reduction as does the design in Figure 6, but provides a suitable armature and magnet design from a practical and mechanical standpoint. The armature weight is 0.55 pound, the armature pull being 400 pounds.

Actually, the weight ratios between the magnets shown in Figures 5 and 7 are even more favorable to the new design,

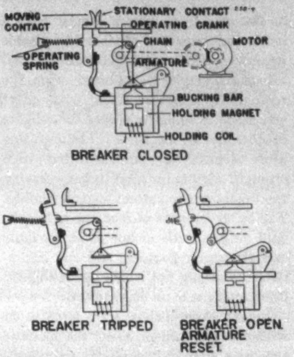
238-9
MOVING CONTACT
STATIONARY CONTACT
OPERATING CRANK
CHAIN
MOTOR
OPERATING SPRING
ARMATURE
BUCKING BAR
HOLDING MAGNET
HOLDING COIL
BREAKER CLOSED
BREAKER TRIPPED
BREAKER OPEN ARMATURE RESET

Figure 9 (above). Mechanical operation of one-quarter-cycle breaker

Figure 10 (below). Typical one-quarter-cycle breaker

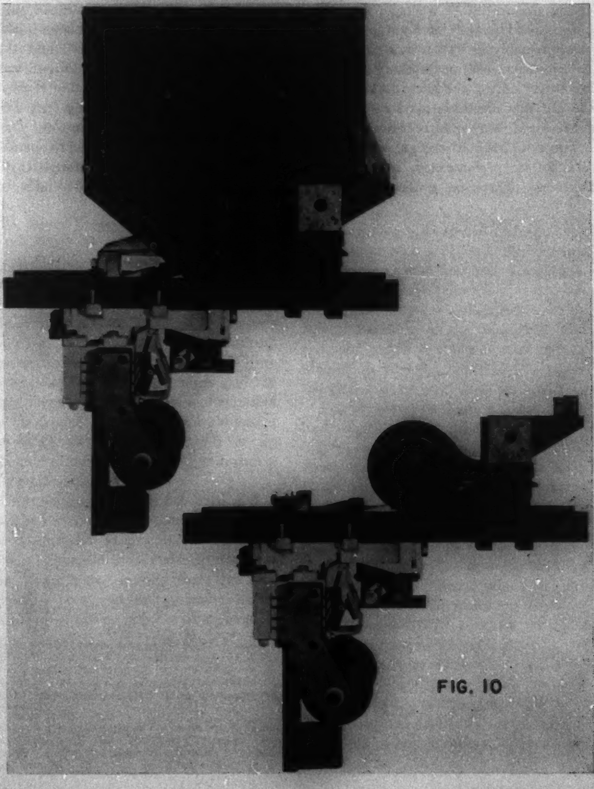
FIG. 10

as the magnet in Figure 5 must have a laminated armature which with suitable mounting details would increase the total armature weight. Combined with the reduction in armature weight, every effort was made to similarly reduce the weights of other moving members. The operating arm consists of furnace-brazed high strength aluminum alloy tubes carrying the 1,600-ampere contact finger assembly. The latter consists of four one-half-inch-wide fingers with nickel silver movable and stationary contacts and one Elkonite-faced arcing tip, which opens slightly later, after the main fingers part.

Initial experiments were conducted with this mechanism, using the blowout structure employed with the previously mentioned latched-in anode breaker. This blowout structure, employed very satisfactorily with the latched-in anode breaker, proved unsatisfactory on the faster breaker. The reason is that with the previous breaker the current reached a value under a certain set of test conditions of 30,000 or 35,000 amperes when arcing was initiated, whereas, with this higher-speed breaker, arcing was initiated at appreciably less than half this value. As a consequence, satisfactory speed of arc travel during the early formation of the arc was unsatisfactory, resulting in distress and delayed current limitation.

The final blowout design, which gave satisfactory performance, is shown in Figure 8. The same general principles, previously used, were maintained, but the primary blowout responsible for initial arc motion was strengthened, and the transfer to the secondary blowout was made much earlier.

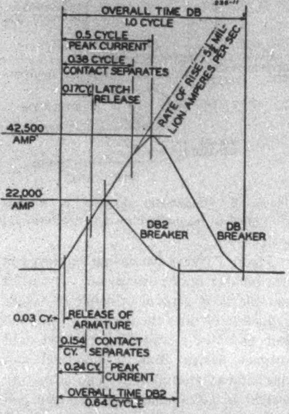

Figure 11. Composite current oscillogram comparing one-half-cycle and one-quarter-cycle anode circuit breakers

The method of obtaining trip-free operation is shown in Figure 9. One compound wound motor is used to operate a six-pole unit, driving, through insulated shafts, the high-speed shafts of small speed reducers, one being mounted on each pole.

The low-speed shaft of each reducer carries a crank and roller over which a small steel chain rides. If the motor is run continuously and the armature is sealed against its magnet, the effect of the chain riding on this crank roller is to cause the breaker to go through an open-close cycle. A limit switch, controlling the motor with dynamic braking, insures this crank roller's stopping at the proper point.

Figure 10 shows a photograph of one complete single-breaker pole. For anode use, the holding-magnet shunt coils are energized at all times from the a-c auxiliary power for its rectifier through a small dry-type rectifier. The six holding coils of the six poles are connected in series, the breaker being tripped from the switchboard by means of the motor mechanism.

Figure 11 is a composite current oscillogram comparing the new breaker with its predecessor. The various time intervals are indicated for the two breakers. The information shown was taken from oscillograms made when testing the breakers by connecting them directly across the terminals of two 3,000-kw 600-volt 300-rpm compensated d-c generators in parallel, no reactance or resistance being in series with the breaker. The rate of rise is approximately the same as experienced in the rectifier station where the breakers are employed.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1942 Supplement to Electrical Engineering—Transactions Section

Recent Developments in Burying Telephone Cables

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THE term "buried cables" has come to mean those underground cables which have no conduit protection. Due to the accelerated demand for such construction in recent years, much effort has been expended in devising methods and developing machinery for burying cables. One of the earlier methods used in this and some foreign countries was to dig a trench by hand alongside the road; unreel the cable from a moving truck, thus laying it beside the trench; work the cable over into the trench by having 30 to 50 men handle it in relays; splice the cable in the trench, and finally backfill the spoil and tamp it by hand. Later variations of this method introduced one or more of the following units of machine equipment:

Power trenching machines.

Caterpillar tractors with trailers to straddle the trench, laying the cable directly from the reel into the trench.

Drag-line or other types of power backfillers.

Power tampers or rollers.

In order to further reduce the number of operations involved, speed up the installation, and reduce the cost, large plow trains have recently been developed which, except for splicing, in ordinary soil complete the job of burying a cable in one pass over the route. The idea of plowing cable into the ground is not new. In fact the great grandfather of all the cable plows was designed by Ezra Cornell long before he established the university. His "ponderous machine" drawn by a "long line of horses" was designed for laying telegraph cable in the early 1840's, but the development was dropped when the simple expedient of carrying wires on poles and insulators was conceived.

The large plow trains recently developed for installing telephone cable are capable of burying either a single cable or a pair of cables together with as many as three properly spaced lightning-protection wires, and of cutting a slot for them as much as 50 inches deep where such a depth is required. To provide the complete plow train has required the design of many pieces of equipment which the word "plow" does not suggest to one's mind. The plows and some of this equipment are discussed in the following paragraphs.

The Plow Train

Arrangement of the equipment in the train varies with the conditions. In Figure 1 two identical plows are included; the front one roots a trench $3^3/_4$ inches wide by 30 to 50 inches deep, thus insuring uninterrupted passage of the following plow which deposits two cables and the lightning shield wires, all properly spaced in the ground. This 100-ton train moves forward at the rate of a brisk walk, laying the cables and wires as it goes, with pauses for reel changes.

Where the ground is not hard and is free of rocks, and thus there is no danger of interruption to the plow from buried obstructions, the train make-up may omit the rooter plow, leaving three tractors, the cable plow, and the two trailers carrying reels of cable. On the other hand, it frequently happens that more difficult plowing conditions exist, or underground obstructions are known to be present, as, for example, with nests of heavy boulders (Figure 2). Here, two or three tractors and one plow will first go over the line one or more times to root the trench. As a separate operation, there will follow one or two tractors pulling a plow followed by a number of cable-reel trailers corresponding to the number of cables to be placed in the trench.

In rooting or plowing, occasionally the train may become stalled. The front tractor will then run ahead with its 76,000-pound single-line capacity winch and "winch out" the train with a two-to-one pull (Figure 3). One of the caterpillar tractors, weighing with full equipment more than 20 tons, has a maximum drawbar pull of 30,000 pounds on the level.

Burying Cable on Steep Grades

Unfortunately, when pulling up hills the available tractor drawbar pull decreases in proportion to the steepness of the grade, since a part of the power is consumed in raising the tractor weight. However, by careful handling of the equipment and generous use of the winch, cable can be plowed up or down hillsides even where the grades are as great as 60 per cent.

The preferred method of operation on mountainsides is to root down the grades, leaving a tractor on top to steady the heavy train with its winch line. In order to obviate any possibility of buried stones wedging at the side of the plowshare when placing the cable, the last pass of the plow doing the rooting work is in the same direction as the cable-laying plow will take. The trusty winch is also located on the hilltop when it is necessary to plow uphill. In contrast with the rooting operation, the cable must be laid uphill as well as downhill. This is because the reel lengths of cable normally used (1,500 to 3,000 feet) will span more than one hill, and it is undesirable to introduce any more splices than necessary in the cable.

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Figure 1. Plow train crossing great plains

The train consists of a 100-horsepower Diesel Caterpillar tractor, a traction-loading cable-reel trailer, two more tractors, a rooter plow, a fourth tractor, a cable-laying plow, and finally two winch-loading cable-reel trailers. Entire train is connected into one unit weighing 100 tons

Figure 2. Rooter train without plow has just passed

Plow comes later and is operated independently where soil obstructions are particularly bad

Figure 3. Pulling plow train out of soft ravine

The heavy steel cables from wire-rope block attached to front of tractor lead ahead to the winch and towing bar of head tractor

Figure 4. Airplane view

Used in selection of cable route

Figure 5. Bulldozer at work

Leveling off sharp dips in uneven ground ahead of plow train

Figure 6. Creek crossing

A roadway for plow train was cut by bulldozer to permit passage through creek banks

Selecting and Preparing the Right of Way

Now that we have seen something of what the plow train is like and how it can winch itself out of difficult situations, let us go back in the sequence of the operations and see how the route for the buried cable is selected, surveyed and explored so that the work of preparing the right of way can be started.

When it has been determined that a buried cable will be required between two points, possible routes are explored to establish the best location, such factors being kept in mind as accessibility, estimated cost of the construction, nature of the terrain, plant of other utilities, cost of right of way, future developments, and so on. In rugged country, this initial survey of the route may be made with the assistance of aerial photography. The airplane survey pictures are carefully studied through special lenses which give a three-dimensional effect in viewing them, and remarkable detail is afforded by the present-day photographic and viewing equipment (Figure 4). The relative heights of trees and buildings stand out with all the clarity of the old-time stereoscope. Since private right of way is generally followed, the use of this ideal method of route selection is often found to be worthwhile.

The tentative route laid out on maps or on the aerial-survey picture is now explored on the ground by engineers. As has been mentioned, the route ordinarily goes across fields, woodlands, mountains, and streams, but always consideration is given to accessibility from the highways and to the other factors which have been discussed. This is important both from the viewpoint of the ease of installing the

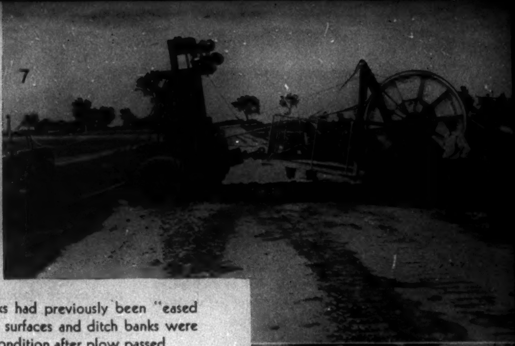

Figure 7. Square crossing of road

Sharp drop of ditch banks had previously been "eased off" by bulldozer. Road surfaces and ditch banks were replaced to original condition after plow passed

Figure 8. "Swamp grousers"

Caterpillar tracks augmented by oak cleats

Figure 9. Steel skid supporting plow tongue in marsh

Because of heavy down thrust on plow tongue a skid $3\frac{1}{2}$ by 11 feet is required to support it

Figure 10. Burying cable in mud

The problem is one of density and depth of mud, weight of equipment, the bearing-up area, and the winch power available

Figure 11. Burying cable in river bed

Power winch on tractor anchored in opposite bank pulls plow train across, depositing cable as it goes

Figure 12. Tractor with four-drum winch

Multidrum winch used for adjusting plow depth, loading cable reels upon trailers, and moving trailers

Figure 13. Relay winch

This winch pulls tongue end of rear trailer into special keeper which acts as a coupling. Paying out this rope permits rear trailer to lay behind the train

cable and that of maintaining it in the future.

Information regarding soil conditions and underground obstructions is very valuable in planning the route. Sometimes oil- and gas-pipe lines are encountered. The plow is sufficiently rugged, and the tractors have ample power to snap a good sized underground pipe in two. This method of striking oil is not to be recommended.

By the use of suitable apparatus, underground pipes can be readily located without excavating. There is commercially available a radio-tube type of underground-pipe locator which indicates the route but not the depth of the buried pipe. To determine its exact location, another electrical detector, developed by the Bell Telephone Laboratories, can be used. This device is so accurate that an underground pipe or cable can be located within less than an inch both laterally and in depth below the surface.

Through use of the information accumulated by the methods mentioned, the line of the proposed buried cable can now be staked out ready for the work crews. Buried boulders, ditches, and other obstacles interfere with the cable-plowing operation. Whatever preparatory work can be done to anticipate delays to the rooting and cable-plowing crews helps to "keep the train moving." At sharp ravines, road ditches, or creeks, the crossings become quite simple if a bulldozer on the front of a tractor (Figure 5) has first cut a roadway through the banks. Creek and river banks particularly require this treatment (Figure 6). It might be noted in passing that at such locations, if there is any danger of the cable being disturbed later by road-construction work or earth washing, the cable is plowed in at full 50-inch depth to afford maximum protection. The sides of steep road ditches are sloped off, not only to facilitate pulling the train across, but also to minimize the tilting of the plow, with the resulting tendency to raise the bottom of the share, thus laying the cable too shallow (Figure 7).

The plow operates satisfactorily across gravel and macadam roads as well as those which are not surfaced. Of course, the roads and ditch banks are restored to their former condition.

Experience indicates that it is not safe to operate along hillsides where the grades are more than about 10 per cent. In attempting to pull around the side of the hill, there is always a tendency for the train to work down grade. For such conditions, the bulldozer is used to good effect cutting out a level roadway.

Of course, in wooded country, a roadway must be prepared for moving the cable reels and equipment along the right of way as well as for clearing a place to plow.

On private right of way an easement on a strip of ground about one rod wide is ordinarily secured, anticipating the possible future need of a second buried cable. A passageway at least 10 feet wide is cleared, and at reel-change points additional width is required to maneuver the equipment. The 10-foot width will permit passage of the train in rooting and plowing. However, unless the cost of clearing an extra 3 feet of right of way is excessive, it is very desirable to have a 13-foot passageway so that the $8^1/_2$-foot-wide tractor can be used for tamping, as will be discussed later. Where practicable, the trees within the proposed passageway are pulled out by the roots in order to eliminate the interference with plowing which the roots would cause.

Crossing Swamps and Streams

Swamps and very soft ground present a difficult problem. For such work the tractor-caterpillar tracks are supplemented with "swamp grousers" (Figure 8), constructed of overhanging, bolted-on oak cleats. The plow tongue is supported by a special steel skid (Figure 9) $3^1/_2$ feet wide and 11 feet long. To provide increased bearing surface for carrying the load of the plow, skids may be added under the 12.75- by 24-inch pneumatic tires. These are in addition to the mud-bearing plates under the plow frame. The cable-reel trailers carry large mud-bearing plates under their frames.

By such expedients as these, the unit bearing pressure on the mud is so reduced for each member of the train that it can be slid over the soft surface placing at the same time the buried cables and wires at the proper distances beneath the surface (Figure 10).

The technique followed in crossing streams is similar in some respects to that used in negotiating marshy ground (Figure 11). If the water is deep, the tractor may find it necessary to detour to shallower water or by the nearest bridge, and it may be found desirable to root across before laying the cable under the stream, whereas, in the soft marsh the rooting may not be necessary.

Important Features in Operation of Plow Train

The caterpillar tractor placed next to the plow in the train is equipped with a four-drum winch (Figure 12) with independent lever controls conveniently located for the tractor driver so that he can exert a pull up to about 6,000 pounds in

Figure 14 (left). Yoke of winch loading trailer lifting a reel of cable

Yoke is pulled up by winch rope from one drum of multidrum tractor winch. Spindle has one foot to go before latches will catch it

Figure 15 (above). Cables emerging from plowshare

In starting a job cable ends are pulled through share and staked before plow moves ahead any one or more of the four winch ropes.

Two of the ropes are used to raise and lower the plowshare, thus adjusting the cable depth. The third rope feeds to a new-type "relay winch" (Figure 13) mounted in the front trailer. On dual-cable jobs this winch pulls the rear trailer up to its coupling under the axle of the front trailer in the operation of changing reels.

The fourth rope from the multidrum winch leads to the arm of the reel-lifting yoke on either trailer as shown in Figure 14. Pulling the yoke forward raises the (possibly 10,000-pound) loaded cable reel into position where the reel spindle is locked in the traveling position. Reversing the operations and slowly paying out the winch line lowers the empty reel from the trailer.

Feeding Cables and Wires Into Plow

In starting a dual-cable job the plowshare is raised to the top position and the cable ends fed in until they emerge from the exit opening (Figure 15). The cable ends are secured against movement along the ground, the share is gradually lowered to depth in the first few feet of travel, and the plow train is moved forward, thus laying the two cables at the desired depth until the reels are empty. After the empty reels are exchanged for full ones, the new cable ends must be fed through the plowshare. This is done by connecting each new length of cable to the one which has just been placed by overlapping the ends and binding them together (Figure 16). As the plow train now starts on the next installation, the spliced ends are guided into the share by two men riding on a platform carried on the front trailer tongue, and a cycle of operations has been completed.

Where lightning-shield wires are required, the reels of wire are carried on the front trailer or on the plow tower as in Figure 7, and they are fed through special ducts in the share from which they are emitted at the desired locations in the ground.

Figure 16. Cable ends clamped ready to enter plow

Each succeeding cable end is clamped to end of cable ahead, in order to pull it into plowshare

Figure 17. Changing reels on front trailer

Train must be broken in order to get new full reel to the front trailer

Changing Reels

Changing the reels as mentioned above is necessary for every cable length. Where one cable is being laid, this is a simple matter, but, where two reels are involved, the changing of the front one presents some difficulties, as can be visualized by referring to Figure 3. The rear trailer completely blocks the operation. In order to leave space between the two trailers, thus permitting the front one to be loaded, the rear trailer is dropped from the train as it moves along, about 50 feet before the cable end leaves the rear reel. This is accomplished by releasing the relay winch, thus disconnecting the rear trailer from the train, just as the engineer of a switching locomotive might drop a car from his train. The front trailer is now accessible from the rear so that the reel can be lowered from it (Figure 17). A tractor with a third trailer now moves a full reel to the front trailer and it is loaded. The relay winch comes into action at this point to pull the rear trailer up to its working position where it is automatically coupled in the train, after which it receives a new full reel of cable.

Lubricating the Cable

The buried cables ordinarily used, range in size from 1 to 2$^{1}/_{2}$ inches outside diameter. However, cables as large as 3.2 inches in diameter may be used by employing a wider share which the plow is designed to accommodate. Usually they are covered with an asphalt-impregnated jute wrapping under which, in gopher-infested territories, there will be steel gopher tape surrounding the conventional lead sheath. In some cases a thermoplastic rubber and burlap covering is used instead of jute.

The asphalt-impregnated coverings develop high coefficients of friction against the steel walls of the rectangular tube through which they pass while in the plowshare. This results in tensions as high as 5,000 pounds in the cable, which is objectionable for electrical as well as mechanical reasons. The tension is reduced to a safe maximum of less than 1,000 pounds by directing a steady oil spray on the cable as it enters the share. This is accomplished by an adaptation of a paint spray gun using compressed air or nitrogen supplied from a cylinder carried on the plow.

Safety Shear Pin

With the powerful tractors pulling in series formation, what happens if the extremely rugged plow hits, let us say, a buried ledge of solid rock? The plow is an integral part of the very heavy briskly moving rooter train with its combined tractor drawbar pull of 200 or 300 horsepower. Under such a condition a safety shear pin of 72,000-pound strength, in the plow tongue, releases the load.

Backfilling and Tamping

In plowing, some soils may be considerably disturbed by the passage of the 3$^{3}/_{4}$-inch-wide share with the extra ducts, familiarly known as "blisters," on the sides to carry the lightning shield wires. As the rear trailer passes over the trench, it drags a V-shaped device which mounds the loose earth over the trench (Figure

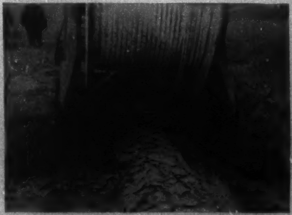

Figure 18. Backfiller

A V-shaped device under rear trailer mounds up disturbed earth over trench in which cable has been deposited

Figure 19. Where buried-cable right of way crosses the high Sierras

In this solid granite, a shallow narrow trench will be prepared by chain blasting

18). Then a good job of tamping is done by running a caterpillar-tractor track along the mound. This is done by the tractor which is used for handling reels.

Signals

The clatter of the Diesel engines makes vocal signals on the job unreliable. A tractor exhaust whistle or an electric horn is connected to a long control rope extending back along the train and so located that it can be reached from convenient points at either side of the train to give signals.

Performance of Cable-Burying Gangs

Under ordinary conditions it is possible to place about 17 trench miles of cable per five-day week with this equipment. This would mean that a foreman with his crew of about eight men, on a dual-cable job, might, in a week, bury 34 miles of cable together with whatever lightning-shield wires are required.

Conditions vary widely from the prairies with their black loam and clay to the steep mountains, or the soft marshes, or to soil sown thick with boulder. The mileage of cable buried daily naturally will correspond to the conditions encountered.

Burying Cable by Other Than Plow Methods

Because of its speed and economy the plow train is used wherever practicable for burying cables. In extremely rough mountainous territory some right of way may be too steep or rocky or too inaccessible, but it is surprising how small relatively is the footage even here which cannot be economically plowed.

For instance, a location on the proposed transcontinental cable line, where there has been some question whether the plow could be used, is in the ten miles of hard salt right of way along the highway adjoining and parallel to the Bonneville automobile-racing flats in Utah. However, in exploratory trials at this location, the plow rooted through the hard salt beds very satisfactorily.

There are some locations in the high Sierras where a relatively small portion of the proposed Sacramento-Reno section of the transcontinental cable run must cross areas of practically solid granite (Figure 19). Here the engineers plan to prepare by chain-blasting a narrow shallow trench in which the cables will be laid and the trench filled with an asphaltic material which will hold its position, keep water out of the trench, and protect the cable.

In a paper such as this, only the major operations and the principal items of equipment can be mentioned. Many others have had to be developed in order to make the use of buried cable broadly applicable. There are the jobs of passing under concrete arterial highways where the pavement cannot be disturbed and the soil may be either earth or rock. There is the matter of finding a way to cross under rivers too swift and full of boulders for submarine cable, having solid granite beds which, of course, cannot be plowed. There is the matter of avoiding buried pipes and other obstructions. These are no small matters. On one 83-mile run there were 91 crossings of oil-pipe lines at the time of the survey. Before the work was done, in a few months, four new pipe lines had been gained and three old ones lost.

All these and many other operations must be planned and executed in an extensive program of burying telephone cables, such as is now being carried on by the Bell system.

A New Instrument for Recording Transient Phenomena

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Synopsis: In many cases it is of great importance to study phenomena which do not occur periodically. Such phenomena, called transients, arise, for example, at the time a short circuit appears in a power line, during the discharge of the condenser of a spot-welding machine, during the starting of electric machinery, and on very many other occasions.

Most of the instruments available thus far for investigation of transients employ the method of film recording, which has the disadvantage of requiring a developing process.

A new transient recorder has been developed, employing magnetic-tape recording as a means of preserving the record of a transient and steadily repeating this record on the screen of an oscilloscope. This method has the advantage that it requires no processing and that the same magnetic carrier can be used continuously without loss of material.

THE investigation of transient phenomena has always been of great interest to physicists and engineers. The observing of transient phenomena presents a major problem, mainly when the time the transient occurs cannot be anticipated, and where the duration of the transient is so short that visual inspection of it is impossible.

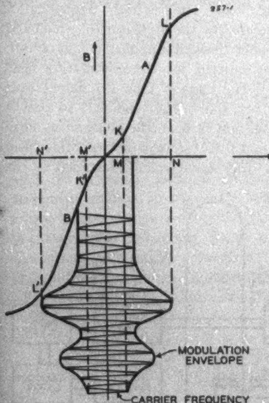

Figure 1. The modulated carrier acting on the magnetization curves of magnetic-recording medium

In most methods known heretofore, recording of the transient phenomena is employed. Usually a permanent record of all occurrences, including the transient under investigation, is made, and the interesting part of the recording is analyzed. Where transient phenomena occur rarely, it seems obviously a waste of material to record continuously the steady-state conditions, only for the benefit of an occasional transient. To meet this difficulty instruments have been developed which will start recording only when the transient occurs. Such instruments require very specialized equipment, particularly since it is usually of great importance to obtain a picture of the complete transient instead of merely one part of it.

Film recording is used in most cases. Mechanical recording, as employed in high speed level—or pen recorders, has not found any wide application, mainly because of the limited frequency range due to the mechanical parts involved.

Different film-recording instruments have been successfully marketed and are now being used in field and laboratory work. The use of film has the great disadvantage that it requires a developing process, and in many cases this requirement makes such an instrument impractical.

These and other difficulties have been overcome by a new transient recording apparatus to be described. This new instrument uses magnetic recording. The principle of magnetic recording and reproduction has been widely discussed in technical literature.[1] The method as used in this instrument, however, involves some novel features which are of interest.

In the magnetic-recording process, small electromagnets known as magnetic heads are subjected to the signal current and produce a magnetic pattern on the recording medium moving between their pole pieces. This magnetic pattern varies in intensity from point to point on the medium, and this variation of the magnetic intensity corresponds to the signal being recorded.

In the reproduction process, this varying magnetic pattern moves past the pole pieces and induces in the pole-piece coils a voltage correlated to the originally recorded signal.

To obliterate the recorded signal it is only necessary to remove the varying magnetic pattern and establish a uniform magnetic state. This is usually accomplished by saturating the magnetic carrier. An alternative way of obliterating is to demagnetize and return the magnetic carrier to its virgin unmagnetized state.

There is, however, one difficulty inherent in the magnetic recording system: it is not possible to reproduce very low frequencies, since the voltage generated in the reproducing head is a function of frequency and becomes small indeed for very low frequencies. On the other hand, transient phenomena often require the recording of not only very low frequencies, but even direct current. Therefore, transient signals in many cases cannot be used directly to actuate the recording head, since it is difficult, if not impossible, in reproducing such recordings to compensate sufficiently for the fall-off at these low frequencies. This difficulty has been overcome by utilizing a carrier frequency which is modulated by the signal to be recorded.

In conjunction with this carrier-recording system, it has been found preferable to obliterate the record by returning the tape to an unmagnetized state. This gives two advantages:

1. It makes unnecessary the use of a d-c polarizing current which normally must be superimposed on the signal to be recorded when obliteration has been obtained by magnetic saturation. This polarizing current, on breaking, would set up its own transient which is unrelated to the investigated transient.

Figure 2. View of obliterating head

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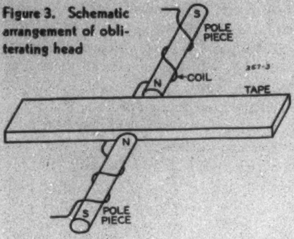
Figure 3. Schematic arrangement of obliterating head



2. When used in conjunction with the carrier system, this obliteration method produces a higher signal-to-noise ratio.

The carrier-recording process may now be analyzed by reference to the two branches A and B of the magnetization curve as shown in Figure 1. These magnetization curves are relatively linear between points K and L and points K^1 and L^1. The unmodulated carrier current is so adjusted that the extreme values of the envelope produced by the modulating signal lie within this linear portion. One half of the modulated carrier acts on magnetization curve A and the other on curve B. The nonlinearity of the magnetization curve near the origin is of little effect since not more than 70 per cent modulation is used. Therefore, the peak values of magnetic induction recorded on the passing medium will be linearly proportional to the peak values of the carrier frequency, thus producing a magnetic facsimile of the envelope.

To obtain a proper reproduction, the reproducing head must be able to take care of the frequency range which is determined by the carrier frequency and the side bands. This, however, can be done without any great difficulty.

The present instrument uses a thin magnetic steel tape as recording medium.

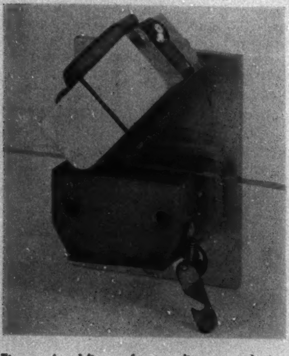


Figure 4. View of recording-reproducing head

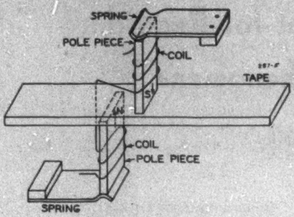


Figure 5. Schematic arrangement of recording-reproducing head

The obliterating is done by a head of novel design. This head consists of two electromagnets oppositely mounted across the width of the tape and polarized so that the opposing ends of the pole pieces have the same polarity, thus producing a diffused magnetic field. This diffused field allows the magnetic-field strength to decrease slowly and gradually as the tape passes by, and since a-c is supplied to the obliterating coil, the magnetic tape emerges completely demagnetized. The obliterating head is shown in Figure 2 photographically and in Figure 3 diagrammatically.

For recording and reproducing, a single standard magnetic head may be used. Such a head is shown photographically in Figure 4 and diagrammatically in Figure 5.

The magnetic-recording method makes it possible to automatically obliterate any previous record and to substitute a new one for it. This unique characteristic of magnetic recording permits continuous carrying on of the recording and obliterating process until a transient phenomenon occurs. As long as steady-state conditions prevail, the record made of them is continuously obliterated, since this can be done without loss of material. The new instrument makes use of an endless magnetic tape on which the signal is continuously recorded and after a short time interval again obliterated. The recording and obliterating process is stopped only when a record of a transient occurrence has been made on the moving tape. The obliterating and recording heads are placed close together in such a way that each portion of the moving tape passes at first through the obliterating head and shortly thereafter through the recording head. The time interval which is required for the tape to complete one cycle, namely to leave the recording head and reach the obliterating head is the available recording time, and this recording time has to be somewhat longer than the maximum duration of the transient to be investigated.

As soon as a transient occurs, a trigger system is actuated which automatically stops the recording process as soon as the tape completes the cycle, as has been just described. The time length of a cycle can be increased by increasing the length of the tape loop. The timing of the trigger circuit is adjustable, in order to conform with this increased time length. Such a trigger circuit is made an integral part of the instrument.

After the transient has thus been recorded on the tape, it will now be periodically reproduced, since the tape is endless. This regular repetition makes it possible to synchronize the horizontal sweep of an oscilloscope with the tape cycle, thus giving a steadily repeating picture on the screen of the oscilloscope, and facilitating observation of the transient.

Figure 6 shows a block diagram of the system. The signal to be investigated is applied to terminals a and b of the modulating circuit (1). The carrier frequency modulated by the signal is amplified by the amplifier (2) and passes then through a filter (3) and switch (4) to the recording head (5). The carrier frequency is generated by the oscillator (6) which also supplies through amplifier (7) an obliterating current for the obliterating head (8) The endless tape (9) moves over the two rollers (10) and (11) so that it passes first through the obliterating head (8) and then through the recording head (5). Any signal which is recorded by the

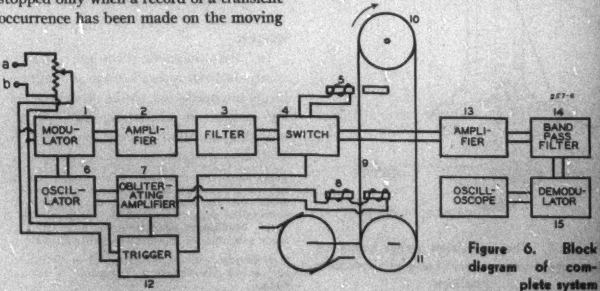


Figure 6. Block diagram of complete system

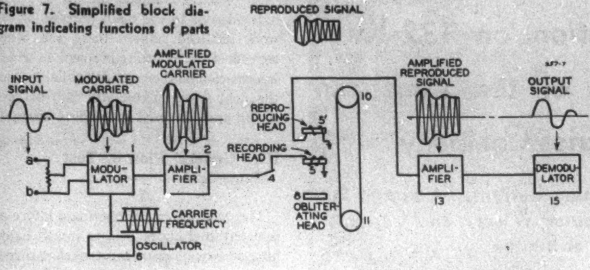


Figure 7. Simplified block diagram indicating functions of parts

recording head remains on a given portion of the tape until this portion reaches the obliterating head, which eliminates any previous signal from the tape and prepares it to accept a new recording. The transient signal is also supplied to a trigger circuit (12) which operates with a time delay. As soon as a transient occurs, the trigger circuit becomes energized, and after a short time interval, slightly shorter than one complete tape cycle, it blocks the obliterating amplifier. Simultaneously, the trigger circuit disconnects, by means of switch (4), the recording circuit from the recording head (5). The system is now ready for reproduction. The reproducing head, which is in this apparatus the same as the recording head, supplies the induced signal to the reproducing amplifier (13) which supplies the signal through a band pass filter (14) to a demodulator (15). The demodulated signal is now reproduced on the oscilloscope.

A simplified block diagram is shown in Figure 7 and illustrates graphically the electric functions of the essential parts of the system.

The different filters serve to eliminate spurious signals.

Great care has to be taken to eliminate phase distortion and amplitude distortion. For proper analysis of a transient phenomenon, the visual picture of the reproduced transient must be identical with the original. Because of this requirement, such an instrument is subject to much more severe restrictions than a sound-recording apparatus, where phase distortions are usually considered unimportant. By proper design of the filter circuit and the other circuit components, amplitude and phase distortion is reduced to an inconsequential minimum for a given frequency range.

The transient recorder in its practical form comprises two units as shown in Figure 8. Unit A contains the modulator, amplifier, and the trigger system. Unit *B* contains the drive mechanism for the tape loop, the magnetic heads (5) and (8), and the power supply. In the drive mechanism a synchronous motor propels by way of a belt the drive roller (11). A flywheel associated with the drive roller assures a uniform speed of the tape. The idler roller (10) can be moved in a slot (16) so as to tighten the tape and also make adjustment possible for tapes of different lengths. The recording-reproducing head may be opened for removal of the tape loops. If it is desirable to preserve an interesting transient on the steel tape, the tape may be removed and kept for any length of time with the signal recorded on it.

Unit *A* with the modulator, amplifier, and trigger has only electronic parts. To assure satisfactory operation independent of line-voltage fluctuation, the oscillator for supplying the carrier frequency and the obliterating signal has been stabilized with respect to frequency and amplitude. The carrier frequency which has been selected for this particular unit is 2,200 cycles. The band pass filters are so designed as to permit the transmission of the carrier frequency and side bands necessary to obtain the desired frequency range, namely 0 to 500 cycles, without phase and amplitude distortion.

The new transient recorder was originally developed to meet the difficulties in investigating transients occurring in the different welding processes, particularly in the condenser-discharge type of spot welding. The frequency range of the instrument has been chosen for this particular application, and the limit of 500 cycles takes care of the frequency components to be expected. The maximum recording time is about 0.2 second and the required input transient voltage is about 2 volts.

Figure 8. View of the instrument

Much work has been done of late with such an instrument in the investigation of welding processes, and it has proven to be very valuable as a laboratory tool. Undoubtedly it will also prove useful for production processes. The instrument may be easily adapted to many other applications. Among these are transient occurrences in power lines, transient phenomena in loud speakers, illumination patterns of photo flash bulbs, shock-excited vibrations of mechanical parts. Many other applications will undoubtedly be found.

The instrument may be designed for different frequency ranges, depending upon the requirements, but still utilizing the same fundamental principle.

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Lightning Investigation on 132-Kv Transmission System of the American Gas and Electric Company

Field Study of Natural Lightning Currents Measured in Towers, Line Wires, and Ground Wires; and Currents and Voltages at Stations

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I. Introduction

THE field study of the characteristics of natural lightning on transmission systems, started some 15 years ago, has been carried on with continued persistence by various investigators up to the present time. On the American Gas and Electric Company 132-kv interconnected system, investigation was started in 1927, and the results obtained have been reported in papers[1–9] before the Institute from time to time, the last appearing in 1937. This present paper presents largely the data obtained from the field work which we have carried on since that time on the above system in an attempt to learn more about the characteristics of natural lightning, particularly lightning currents and rates of voltage rise, as they affect transmission lines and equipment. It includes field data for the past four years, combined in some cases with previous field data in order to make the records complete and inclusive.

II. Purpose and Scope of Investigation

Without attempting to summarize the various phases of the lightning problem to which field research has been directed in past years, it is sufficient to state that the past four years' work in our system has been confined to furthering information along the following general lines of attack:

1. A study of the distribution of lightning currents in parts of the transmission system such as ground wires, line wires, towers, counterpoises, and ground rods.
2. The effect of different methods and technique in grounding, such as with counterpoises and ground rods.
3. Study of lightning voltage, magnitude and wave shapes appearing at stations, as an aid in co-ordination of station insulation and protection.

Table I. Lightning-Measuring Instruments—Location—Number and Magnitude of Records Obtained on 132-Kv System—1933 to 1941 Inclusive

Ref.	Instrument Location	Instrument Years	Number of Records	Record Magnitude		
				Maximum	Median*	Minimum†
1	Tower legs above ground	3,163	991	25,000	5,300	1,000 Amperes
2	Tower legs below ground	344	54	12,000	2,100	1,000 Amperes
3	Tower-leg sections	244	49	24,000	3,500	1,000 Amperes
4	Tower-leg braces	180	24	6,900	1,250	700 Amperes
5	Tower arms	1,977	372	26,000	2,800	1,000 Amperes
6	Tower-arm braces	237	45	13,600	2,000	600 Amperes
7	Tower lightning rods	560	70	82,000	22,000	12,000 Amperes
8	Counterpoises	937	662	24,300	4,000	200 Amperes
9	Counterpoise tie between tower legs	57	40	10,200	1,250	200 Amperes
10	Ground rods at towers	169	31	11,900	3,000	200 Amperes
11	Ground wires at towers	545	643	70,000	5,000	1,000 Amperes
12	Ground wires at stations	56	53	11,700	2,800	1,000 Amperes
13	Conds. at towers	274	261	29,000	1,800	1,000 Amperes
14	Conds. at stations	117	170	11,400	1,800	1,000 Amperes
15	Wave-slope indicator	77	539	600	200	10 Kv per µsec
16	Surge-voltage recorder at stations	18	151	525	160	120 Kv
	Totals	8,955	4,158			

*50 per cent of records at or above this value †Min. recording sensitivity of instruments.

Incidental to the main purpose of the work outlined above, there are other aspects of the lightning problem on which information has been made available from the field data. These will also be presented and discussed as the data warrant.

III. General Plan of Test Procedure

The general plan of obtaining information outlined above was to install lightning-measuring instruments such as surge-crest ammeters, surge-voltage recorders, and wave-slope indicators on some lightning-infested parts of our 132-kv interconnected system. The lines on which the major part of the data presented and discussed here were obtained are the 132-kv lines of the Appalachian Electric Power Company (Glenlyn-Roanoke), the Indiana and Michigan Electric Company (South Bend-Michigan City), The Ohio Power Company (Philo-Canton), and the Atlantic City Electric Company (Deepwater-Pleasantville).

Instruments

The surge crest ammeter[10] using magnetic links was used to determine lightning currents. At stations the surge-voltage recorder[11] was used for determining lightning voltages and the wave-slope indicator rather extensively to determine the lightning-voltage rates of rise.

The wave-slope indicator is based on the following theory, using the well known elements of the electric circuit q, i, and e:

$$q = ec = \int i\,dt$$

$$c\,de = i\,dt \text{ and } de/dt = i/c$$

Maximum i thus gives maximum de/dt. A wiring diagram of the instrument as used in service is shown in Figure 1A; and field installations in Figures 1B and 1C.

A typical field setup of magnetic links to determine lightning currents at various points on the transmission system is

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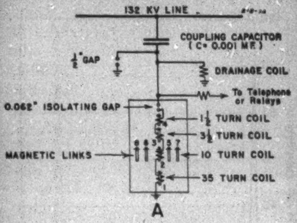
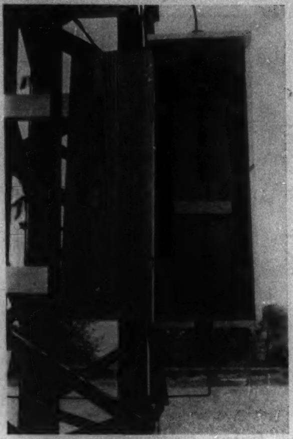
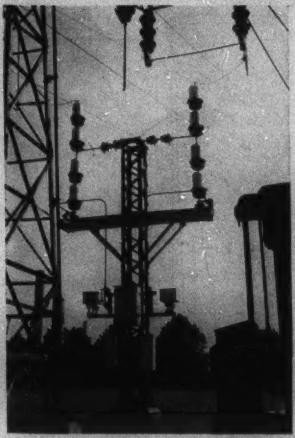
The field investigation work on which this paper is based was undertaken and planned in co-operation with engineers of the General Electric Company, who furnished the instruments and aided in correlating the data. The authors acknowledge the assistance of the field organizations of the Appalachian Electric Power Company, The Ohio Power Company, Indiana and Michigan Electric Company, and Atlantic City Electric Company, in installing and servicing the instruments in the field.



A. Schematic diagram of wave-slope indicator showing capacitance coupling to 132-kv line and surge-crest ammeter links to measure current



B. Wave-slope indicator installed in weatherproof housing in the field



C. Two wave-slope indicators installed on the Roanoke–Reusens line near base of steel structure which carries two 132-kv coupling capacitors for communication service

Figure 1. Wave-slope indicator for determining rates of voltage change

shown in Figure 2. Therein is shown the approximate general location of instruments as used in the field. It is not to be interpreted that every tower under investigation had all instruments shown, but where used, they were located as indicated in the figure.

A concentration of instruments was made at several sections of the line where counterpoises (500 feet and continuous) were installed on the Glenlyn-Roanoke line, and also at some five substations to determine currents in line wires and ground wires, and the wave slope of incoming voltages to which equipment installed in the stations would be subjected.

The data given and discussed below have been obtained from field records as indicated in Table I. Therein it is shown that some 8,955 instrument years have been involved during the nine years in this investigation and have produced a total of 4,158 records. The distribution of these records among various parts of the tower structure, line wires, ground wires, counterpoises, and so forth, as well as maximum, minimum, and median values is clearly shown in the table.

Throughout the investigation, all instruments were serviced approximately once each two weeks to enable the best possible segregation of records without burdensome field servicing. In addition, instruments were frequently serviced after severe lightning storms which were known to have taken place in the general vicinity of the test stations.

IV. Lightning Records and Data

Current Magnitudes

Lightning-Stroke Current

A determination of current in the lightning stroke has been arrived at in four ways:

1. By multiplying the current measured in one leg of a tower, where only single-leg measurements were attempted, by four (these towers have four legs).

2. By adding the separate currents measured in all four legs of a tower.

3. By direct measurement of the current in a lightning rod placed at the top of the tower.

4. By adding the measured ground-wire currents flowing toward a stroke in midspan.

These data have been plotted in Figure 3 as a magnitude-frequency curve. The measured tower-leg values have been increased by 50 per cent to account for the unmeasured cross-brace current, a procedure previously justified.

Comparing these data with those previously published,[9] the range of indicated stroke currents is in substantial agreement. The maximum current here indicated is, however, 150,000 amperes by the single-leg-measurement method compared with the previously reported maximum of 220,000 amperes by the method of adding adjacent tower currents. Direct measurement of current in the lightning rod showed a maximum of 82,000 amperes and measurement in ground wires 110,000 amperes. As both of these methods of measurement resulted in partially saturated links at high currents, it is believed the maximum stroke current is slightly higher than the above 110,000 but not much in excess of 130,000 amperes, which in turn is only slightly higher than the current measured in a single tower.

It is rather interesting to note that the method of measuring currents in one tower leg and multiplying by four gives results quite comparable to the other methods except that it does show a slightly higher maximum current.

Ground-Wire Currents

Measurements of ground-wire currents were made in the line sections some distance out from the station, as well as at the first tower out from the station, and at the station itself. Some 700 separate current records were obtained on ground wires, 643 being of sufficient magnitude and certainty to warrant analysis.

Figure 4 shows the magnitude and frequency of these currents at the three locations (line, near station, and at station). In the line sections the maximum current was 70,000 amperes; at the first tower from a station, 36,000 amperes, and on the ground wire coming into the station, 12,000 amperes. Where measurements were made directly at the station, they were in all cases on one of three fanned-out ground wires extending from the station to the first tower, where single ground-wire construction over the line started.

That the single-ground-wire and three-ground-wire current curves (Figure 4) cross at approximately 7,500 amperes has no significance and is caused by the fact that the data do not attempt to indicate simultaneous current records on the ground wires, but rather the general magnitude and distribution which may be

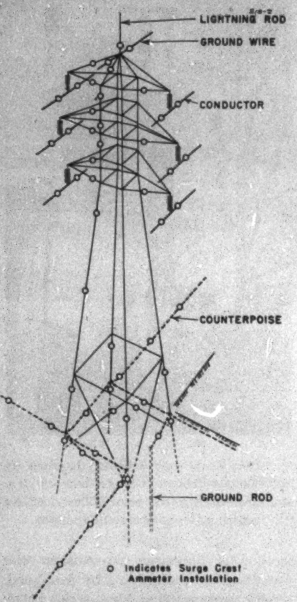


Figure 2. Schematic diagram of tower showing location of magnetic links on various parts of the tower and conductors

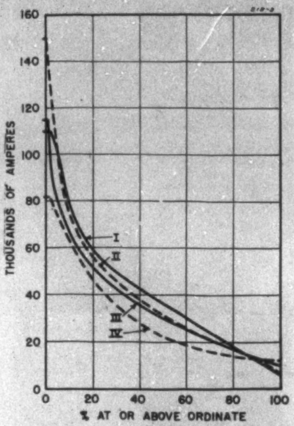


Figure 3. Lightning-stroke currents

Magnitude and frequency determined as follows:

I. Summation of ground-wire currents—51 records

II. Total tower current by multiplying single-leg readings by four—104 tower records

III. Total tower current by addition of four-leg currents—142 tower records

IV. Currents measured directly in tower-top lightning rods—52 records

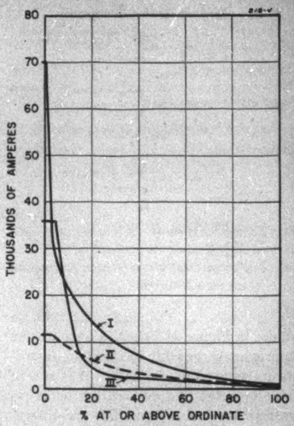


Figure 4. Lightning currents in overhead ground wires

I. At line towers—591 records

II. Currents in one of three ground wires between station and first tower out—30 records

III. In single ground wire on the line side of the first tower from station—22 records

expected where this type of ground-wire construction is employed near a station.

Lightning Currents in Line Wires

The determination of currents in line wires was undertaken for two reasons:

1. In an attempt to correlate the lightning currents and surge impedance of the line with the lightning voltage expected with such currents.
2. To determine the magnitude of lightning currents which might enter a station from the line and would have to be handled by the protective devices.

Over 300 records in line conductors have been obtained, 154 measured at the station and 172 on line towers some distance away. The magnitude and frequency of these currents are shown in Figure 5. The maximum current out on the line is 29,000 amperes and at the station 11,500 amperes. Ten per cent of the measured currents are 6,500 amperes or less at the line towers and 3,000 or less at the station.

Table A

Instrument Location	Amperes Maximum	Amperes 10% of Values at or Above	Corresponding Kv of Line Wire Maximum	Corresponding Kv of Line Wire 10% of Values at or Above
On the line	29,000	6,500	11,600	2,400
At stations	11,500	3,000	6,600	1,200

If it is assumed that these measured lightning currents can be simply multiplied by the line-surge impedance (taken as 400 ohms), to obtain the conductor voltage, then the voltages of the line conductors would be as shown in Table A.

The line-current data, analyzed as above, give indicated conductor voltages which seem rather fantastic under the conditions of operation. The magnitude of currents in the line conductors at stations, however, correlates quite closely with the measured currents in lightning arresters as has been reported previously.[12]

Currents in Towers, Counterpoises and Ground Rods

Currents in counterpoises as measured at the point where they attach to tower legs were measured in five different types or lengths of counterpoises, namely, 40, 150, 250, 500 feet, and continuous. The continuous counterpoises were installed between towers where the tower spacing was approximately 1,500 feet or less. The counterpoise currents recorded on the above basis are shown graphically in Figure 6, from which it will be noted that the currents carried by the counterpoise increase with length, although not in direct proportion to the length. The most searching type of test would, of course, be one where all these counterpoises of various lengths were attached to the same tower and therefore affected by the same stroke current, but since a setup of this kind was not practical, the comparison of data has been made on actual measured currents. This method does not evaluate the variation of stroke current which initiates current in the counterpoise. However, since the data cover some eight years of field tests, the varying magnitude of stroke currents has probably been averaged out to some extent so that the data given indicate to a large degree the relative benefits of the counterpoise length.

Comparative data on a selected group of towers where 500-foot and continuous counterpoises were installed, and, in addition, tie connections between the four tower legs, are given in Figure 7. The tower currents have been obtained by four-leg readings increased by 50 per cent for the shunting effect of tower bracing, and show a maximum of 79,000 amperes. The 500-foot and continuous counterpoises show maximum currents

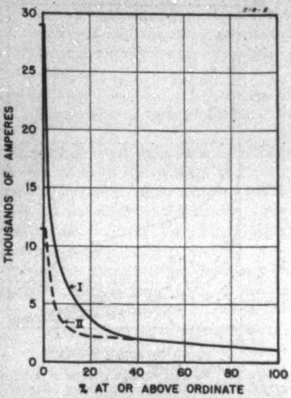


Figure 5. Lightning currents in conductors

I. Conductor currents at line towers—172 records
II. Conductor currents at station entrance—154 records

and frequency distribution of approximately the same magnitude. This would be expected if the continuous counterpoise can be considered as effective only for about half the span (or 400 to 750 feet). In fact, the agreement of curves III and IV seems to indicate that increasing the counterpoise much beyond 500 feet is not particularly effective.

One important feature brought out by these data is the records of currents obtained in the buried ground or counterpoise wire which ties the four tower legs together at the base of the tower. This connection consisted of a $^{1}/_{16}$-inch by 2-inch flat iron strap buried 18 inches in the ground and extending around the base of the tower, as shown in the sketch of Figure 2. The current carried by these tie connections is shown in curve V of Figure 7, a maximum of 10,200 amperes

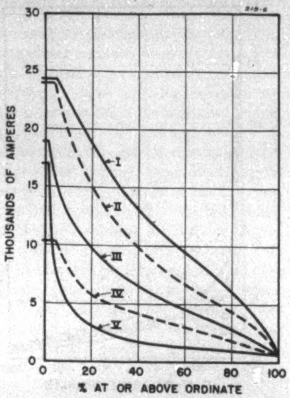


Figure 6. Lightning currents in counterpoises

Measured at point of junction to tower leg
I. In continuous counterpoises—22 records
II. In 500-foot counterpoises—23 records
III. In 150-foot counterpoises—79 records
IV. In 250-foot counterpoises—22 records
V. In 40-foot counterpoises—67 records

being indicated. While it might seem, in tying a counterpoise direct to one leg of a conventional four-leg tower, that the tower cross bracing would be effective in quickly distributing the lightning currents to the other legs of the tower, such does not seem to be the case (see also Figure 11). It is strongly indicated that, where counterpoises are used, means should be taken to make direct underground connections to all tower legs to obtain the maximum benefits from an even, uniform, and quick distribution of current to the various tower structural members.

A comparison of tower and counterpoise currents with 40-foot and 150-foot counterpoises attached is shown in Figure 8. Here, the benefits of a number

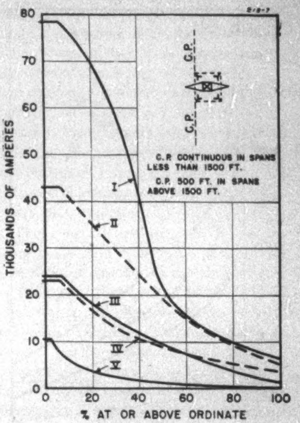


Figure 7. Lightning currents in towers and counterpoises

I. Total tower currents—15 records
II. Summation of currents in counterpoises both sides of tower—15 records
III. In single 500-foot counterpoises—14 records
IV. In single countinuous counterpoises—16 records
V. In buried cross-tie connections between tower legs at towers—40 records

of short counterpoises are indicated as compared with using the same length of buried wire as a single counterpoise. Taking the median values in Figure 8, 80 feet of counterpoise in two short sections carry 5,000 amperes, and 300 feet of the 150-foot counterpoise in two sections, 17,000 amperes, or 3.8 times the length, carry 3.2 times the current. When comparing maximum currents, the results show still greater efficiency for the short counterpoise. Again comparing median values for the 40-foot counterpoise (Figure 8) with the 500-foot counterpoises of Figure 7, the ratio of increase in length is 5.9, and the ratio of currents 2, thus indicating considerably less effectiveness with the single long counterpoise. Although these records were not obtained at the same tower under the same lightning condition, the comparison of counterpoise efficiency given above seems justified, as the median tower currents of 30,000 for the short counterpoise and 22,000 for the long single one, and similar maximum currents are reasonably comparable.

Table II*

		Lightning-Arrester Location and Amperes					
Station	Line Amperes	Line Entrance	On Bus	Transformer Terminal	Bus Kv	Apparent Line-Surge Impedance (Ohms)	Rate of Voltage Rise at Station Entrance (Kilovolts per Microsecond)
Roanoke	2,000	—	500	None	472	236	212
	3,200	—	None	350	525	164	—
	2,200	—	1,050	300	397	180	172
	2,400	—	450	None	300	125	92
	2,000	—	None	None	233	117	164
Claytor	2,000	—	None	—	224	112	260
Fieldale	None	—	450	None	428	—	240
	3,000	—	None	None	428	143	275
	2,000	—	None	None	330	115	350
Glenlyn	2,800	350	—	—	—	—	300

* Dashes (—) in table indicate no installation of equipment nor measuring devices to obtain records.

Tower-Arm and Arm-Brace Currents

The question has been raised[13] regarding the magnitude of stray currents in the tower such as in braces, arms,

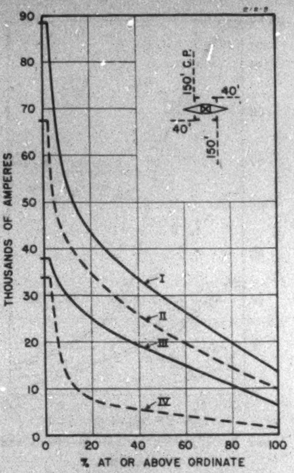


Figure 8. Lightning currents in towers and radial counterpoises—42 records

- I. Total current in tower including legs and braces
- II. Total current in two 40- and two 150-foot counterpoises
- III. Total current in two 150-foot counterpoises
- IV. Total current in two 40-foot counterpoises

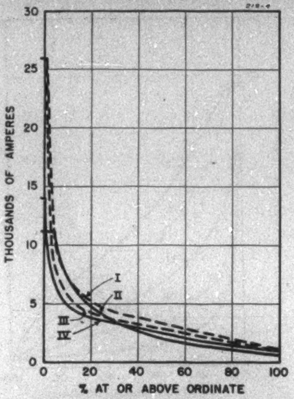


Figure 9. Lightning currents in tower structural members

- I. In arms where line flashover occurred—113 records
- II. In arm braces—48 records
- III. In all arms—353 records
- IV. In tower arms without line flashover—240 records

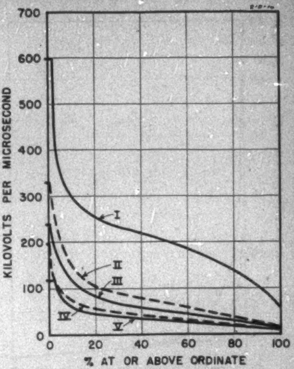


Figure 10. Rate of voltage change at stations

Measured at station entrance

- I. Pure lightning surges (link 5)—45 records
- II. Determinations from link 4—267 records
- III. Determination from link 3—434 records
- IV. Determinations from link 2—492 records
- V. Determinations from link 1—41 records

Records from links 1 to 4 inclusive include lightning and switching surge voltages

hanger bars, and so on. Some data on this question are given in Figure 9 where maximum currents in tower arms are shown as 26,000 amperes and the median currents in this structural member from 2,000 to 3,500 amperes. Detailed analysis of the records shows that most of these currents, except possibly the highest, are leg currents shunted by the bracing members.

Lightning Voltages at Stations

Magnitudes

In an attempt to correlate lightning effects at stations, surge recorders were located on the three phases at three 132-kv stations and recorded the station bus voltages to ground. Lightning currents on the lines and through station lightning arresters were also recorded. A summary of ten records correlating this information is given in Table II. It will be noted that the highest station voltage recorded was 525 kv, the lowest in this group 224 kv, and the average value 334 kv. At all of these stations, arresters having a gap breakdown and *IR* discharge at 3,000 amperes of approximately 388 kv and 346 kv were installed.

It will be noted that correlation between recorded bus voltage and arrester characteristics for the current measured is rather good, thus indicating, not only that the arresters are affording protection, but also that they are operating with their expected performance characteristics.

In this table are also shown the maximum recorded incoming lightning currents on the line wires. An attempt, however, to use line-surge impedance and current, say 3,000 amperes by 400 ohms, to determine the bus voltage indicates 1,200 kv which requires some further study or interpretation to properly correlate with other records.

Using the recorded bus voltage and line currents in an attempt to determine the line-surge impedance, an equivalent value is given in the seventh column of Table II, ranging from 112 to 236 ohms, with an average of approximately 150 ohms.

Rates of Voltage Rise

The wave-slope indicator used at 26 locations, and coupled to the 132-kv telephone and relay capacitors, in five different 132-kv stations yielded records of some 492 separate voltage occurrences from which the rate of rise of voltage at the station was determined. The data are summarized in Figure 10. The five separate curves shown are based on the rate of voltage rise determined by the individual calibration of magnetic links in the first five positions of the wave-slope indicator (see Figure 1).

After the first records were obtained from this test setup, it was believed that some of the results were affected by switching surges. A field check verified this point, and it should, therefore, be pointed out that links 1, 2, 3, and 4 are affected by switching surges so that the data presented from link readings 1 to 4 inclusive are a combination of characteristics of lightning and switching-surge voltages. The data from link 5, however, appear to be confined solely to lightning-voltage surges entering the station and, therefore, may be taken as indicative of lightning voltages only, coming in from the transmission lines.

It will be noted that the maximum rate of rise of pure lightning surges is 600 kv per microsecond, the minimum 60 kv per microsecond, and the median or 50 per cent value approximately 200 kv per microsecond. While these data in Figure 5 were obtained from only 45 records, it is believed they are the first of their kind giving any actually measured rates of lightning-voltage rise at the stations where protective devices are used extensively to protect the station equipment. It is worth-while in passing to note that the recorded rates of lightning-voltage rise are somewhat less than the generally accepted 1,000 kv per microsecond on which some present-day test work is often predicated.

The rates of voltage rise for switching surges are considerably lower than for lightning voltages and show a maximum of 325 kv per microsecond, a minimum of 10 kv, and a median value of approximately 50 kv.

V. Discussion of Special Cases

Having obtained some 4,000 individual records of lightning currents and voltages, it has been impossible to present each one separately and discuss it. For this reason, most of the data already presented here have been in curve form, showing the magnitude and frequency of currents which, in many cases, of course, have not been simultaneously recorded with the other correlated data presented. It is, therefore, of interest to discuss a few special cases where readings were obtained simultaneously in various parts of the circuit considered.

CURRENTS IN COUNTERPOISES, GROUND RODS, AND TOWER LEGS

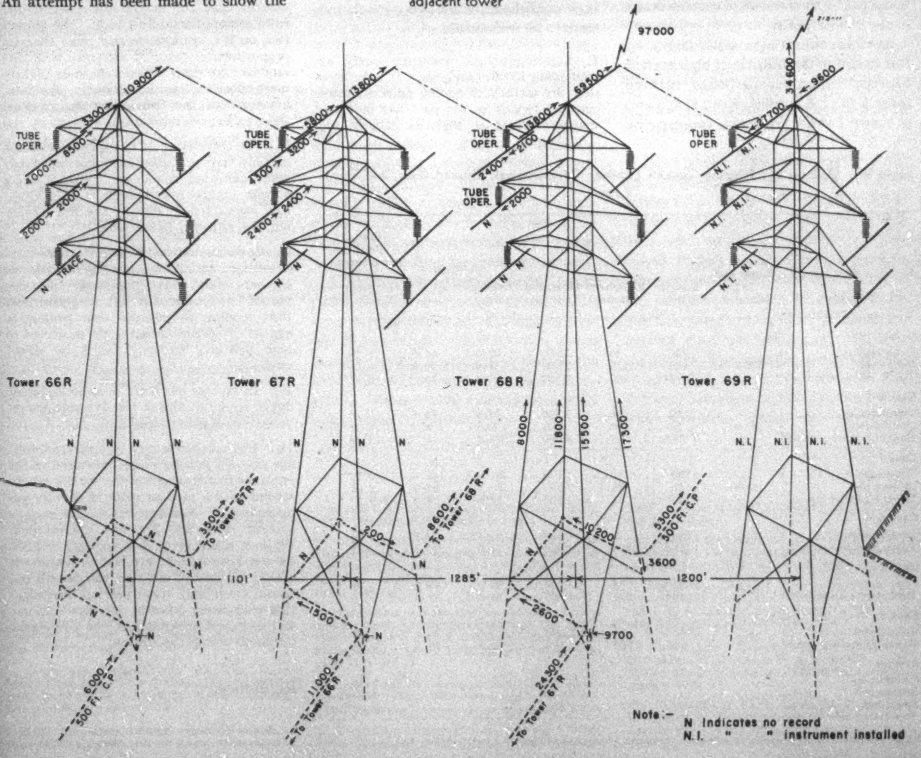
In Table III are presented the simultaneous readings recorded in counterpoise, ground-rod, and single-tower leg. An attempt has been made to show the comparative value of these three elements in carrying lightning current. The resistances recorded were measured with the customary d-c ground-measuring instrument. (The tower-leg resistance was taken as four times the measured ground resistance of the four-leg tower.) If these three elements are equally effective in carrying current, there should be recorded in each a current in exact proportion to its conductivity. The last column of the table showing the ratio of current to conductance shows the deviation from this law. It will be noted that the counterpoise carries an average current 74.5 per cent of that expected, and deviations from this value in the four cases are not great. The ground rod is very much more erratic, carrying current ranging from 15.6 per cent to 180 per cent of the value based on conductivity alone. This, however, may be accounted for by the lower value being recorded in a 10-foot rod, and the higher value in a 60-foot rod where the earth was not so much affected by the presence of the counterpoise.

In the case of tower legs, omitting the two extreme values of 6.10 and 0.76, the general average is approximately 2.25 times that expected.

The general conclusion to be drawn from these data appears to be that the tower leg on account of the large extent of its exposure to ground is much more effective in carrying lightning current than either the ground rod or the counterpoise. Further, it appears that a counterpoise of 250-foot length, at least, does not carry current in proportion to its conductivity. This is completely in accord with past observations and theory, which indicate that when the counterpoise much exceeds 200 feet or so, its effectiveness does not increase anywhere near in direct proportion to length.

Figure 11. Typical record of lightning strokes to overhead ground wire on 132-kv Glenlyn–Roanoke line showing distribution of lightning currents in grounded structure, counterpoises, and in line conductors

Note that two strokes have occurred; one to the ground wire in mid-span, and one to an adjacent tower



Tower 66R
TUBE OPER.
10900
3300
8500
4000
2000
2000
TRACE
N
3500 To Tower 67R
6000 500 Ft. C.P.
1101'
Tower 67R
13800
5800
3200
4300
2400
2400
200
8600 To Tower 68R
1500
11000 To Tower 66R
1285'
Tower 68R
97000
69500
13800
2100
2400
2000
8000
11800
15500
17300
10200
5300 500 Ft. C.P.
3600
2600
9700
24300 To Tower 67R
1200'
Tower 69R
34600
9600
27700
N.I.
Note:- N Indicates no record
N.I. " " instrument installed

CURRENTS IN TOWERS, GROUND WIRES, LINE WIRES, AND COUNTERPOISES

A typical case of a severe lightning stroke to the overhead ground wire between towers, and involving a stroke of smaller magnitude to an adjacent tower, is shown in Figure 11. The lightning-stroke current has been determined as 97,000 amperes from the sum of the two ground-wire currents. This is a particularly interesting stroke as it shows currents in the ground-wire on one side of the stroke of 69,500 amperes and on the other side of 27,700 amperes. Assuming that the smaller stroke to tower 69*R* occurred after that to 68*R*, the 69,500 amperes in the ground wire might be approximately half the total stroke current, indicated above as 97,000 amperes, or in other words, the stroke current might have been twice 69,500 or 139,000 amperes. The smaller ground-wire current of 27,700 amperes from tower 69*R* may be accounted for by the demagnetizing effect of ground-wire current flowing into the stroke at tower 69*R*. This sequence of events seems to be borne out by the reversal of current shown in the counterpoise.

Another interesting point shown by this record is the transfer of high current between buried counterpoises between tower legs. A current of 10,200 amperes is shown flowing from one counterpoise tower leg to one non-counterpoise leg. So far as currents in adjacent towers are concerned, these seem to indicate that, in this case at least, the stroke current was drawn through only the tower structures quite local to the point of the stroke.

This record also shows the current in the line wires of some 2,000 to 8,500 amperes. If this is analyzed on the basis of line-surge impedance, extremely high voltage on the line conductors are indicated. The fact that protector tube operations occurred at the four towers further confirms the high voltage occurring at these consecutive towers. As a matter of fact, tube operation occurred in this case at six consecutive towers.

Other strokes to the ground wire similar to the above have been observed showing the same general trend as discussed in the typical case given.

VI. Summary and Conclusions

Based on the data obtained during the past four years in combination with other similar data, much of which has already been published, the following conclusions seem to be warranted:

1. Lightning-stroke currents rarely exceed some 150,000 amperes. It is believed that the method of adding adjacent tower currents (which in the past has indicated stroke currents as high as 220,000 amperes) to determine the stroke current gives results which are probably too high.

Table III. Division of Lightning Currents Between Counterpoises, Ground Rods, and Tower Legs

Structure	Tower and Leg Number	Length (Feet)	Resistance (Ohms)	Measured Amperes	Per Cent		Ratio of Per Cent Current to Per Cent Conductivity
					Conductivity	Current	
Case 1							
Counterpoise	25-4	250	15	7,800	82.3	64.5	0.78
Ground rod		60	110	2,500	11.4	20.5	1.80
Tower leg below ground		10*	200	1,800	6.3	15.	2.37
Case 2							
Counterpoise	29-2	250	14	3,300	69.5	51.	0.73
Ground rod		10	100	200	9.8	3.	0.31
Tower leg below ground		10*	48	3,000	20.7	46.	2.23
Case 3							
Counterpoise	29-3	250	10	5,100	61.0	43.5	0.72
Ground rod		40	25	3,300	26.2	28.3	1.08
Tower leg below ground		10*	48	3,300	12.8	28.2	2.20
Case 4							
Counterpoise	27-2	250	13	7,100	94.1	70.0	0.74
Tower leg below ground		10*	200	3,000	4.9	30.0	6.1
Case 5							
Ground rod	27-1	53	68	3,900	85.0	68.5	0.82
Tower leg below ground		10*	200	1,800	15.0	31.5	2.10
Case 6							
Ground rod	27-4	60	35	7,600	75.0	81.0	1.09
Tower leg below ground		10*	200	1,800	25.0	19.0	0.76
Average							
Counterpoise							0.74
Ground rod							1.00
Tower leg							3.00†

*Approximate. †2.25 is probably a better average on account of the high 6.1 value in case 4.

2. A total tower current of 150,000 amperes has been indicated as a maximum with 10 per cent above 80,000 amperes. This compares with previous data[8] of 100,000 amperes and 45,000 amperes respectively.

3. The maximum benefits of counterpoises are obtained with several short ones rather than by using the same total length in a single counterpoise.

4. To aid in the transfer of current from the counterpoise to tower legs, the bonding of tower legs together near the ground line (in addition to relying solely on the structural bracing of the tower) seems advisable (see Figure 11).

5. Currents circulating in the tower members such as crossarms, hanger bars, and so on, are comparatively small in each member and are the result of shunting current out of the main tower-leg path.

6. The current shunted from the tower legs by the bracing members in the conventional four-leg tower has been shown to average about 50 per cent of the measured tower-leg current. (Detailed data not presented here.)

7. Based on the measured d-c resistance, the counterpoise is less effective in carrying current than the normal tower leg in the ratio of approximately 1 to 3. The ground rod, on the average, carried more than its proportionate share of current, with the tendency for deep driven rods to be slightly more effective than shorter ones. The data, however, are too limited for one to draw this as a definite conclusion.

8. The correlation of measured conductor currents with probable line-surge impedance gives indicated conductor voltages which appear highly questionable. This situation appears not only on the line proper but also adjacent to stations.

9. Currents measured in line conductors at stations are of the same general order as currents which have previously been reported[12] as measured in lightning arresters, thus tending to indicate that protective equipment at the station is not subjected to high lightning currents unless by direct stroke.

10. In no case has there been an apparent direct stroke to any of the five stations involved in this investigation.

11. The maximum rate of measured voltage rise of lightning surges measured at the entrance to a station was 600 kv per microsecond with a median value of 200 kv per microsecond. Ten per cent of the records showed over 300 kv per microsecond. While it might seem that voltages of 1,000 kv per microsecond are not prevalent, it should be pointed out that the records obtained cover only three years of investigation work, were taken at only five stations, and were not measured at the equipment terminals within the station.

References

1. SURGE-VOLTAGE INVESTIGATION ON 132-KV TRANSMISSION LINES OF THE AMERICAN GAS AND

Progress in Development of Trolley-Coach Overhead Reflected in Higher Service Standards

L. W. BIRCH
MEMBER AIEE

Synopsis: The expansion of trolley coach operation to include 50 transit systems in the United States and Canada has been greatly helped by the progress made in the development of overhead distribution materials. Beginning with the installation of the first transit-type trolley coaches in Salt Lake City in 1928, the inadequacy and limitations of the earlier designs of equipment were recognized. Since 1928 it has been necessary to add many new devices and redesign many old devices to provide better reliability and permit of greater flexibility of operation. The development of trolley-coach overhead materials has been concentrated mainly on

1. Current collection
2. Hangers and insulation
3. Curve materials
4. Turnouts and crossovers

The improved current collection equipment incorporating the carbon-insert shoe has been responsible for the reduction of wear on fittings and trolley wire. The carbon-insert shoe has also been responsible for the complete elimination of trolley-wire lubrication formerly required for the all-metallic shoe collector.

Improvements in insulated hangers have resulted in a more dependable distribution system and have been responsible for the elimination of considerable secondary insulation formerly inserted in the supporting span wire between positive and negative trolley wires.

The curve segment, available in any degree, is replacing the conventional pull-over arrangement on all trolley-coach overhead systems. Today the conventional pull-over is only installed on curves used jointly by both street car and trolley coach.

Special assemblies for turnouts and crossovers are constructed with the same insulating units. These interchangeable units have been strengthened both mechanically and electrically to meet the severe service on heavy lines. Automatic electrical equipment has been developed for all classes of turnouts and has practically replaced the older types of manually operated devices.

Today's trolley-coach overhead is both mechanically and electrically stronger than the overhead available a few years ago. In combination with the carbon-insert shoe there is less fitting and trolley-wire wear, less dewirements, and less maintenance cost.

A VEHICLE propelled by electric energy collected from a trolley-wire system has this advantage—that power is always available. It is not necessary to refill a tank or tender. Trolley-coach operation is of this type and is successful because the overhead distribution system is capable of delivering continuous power.

As electrical engineers we are all interested in electric transportation. The trolley-coach overhead distribution system is part of a $70,000,000 trolley-coach investment which, in turn, is part of the mass transportation systems on 50 properties. These systems represent a total investment of $700,000,000.

Problems of Operation and Design

Primarily a trolley-coach overhead system differs from a street-car distribution system in that there is no rail return. An aerial negative contact wire replaces the rail. The support and insulation of two aerial contact wires for operation with mobile current collectors presented several interesting problems. These include:

1. *The current collector, itself.* Numerous single-pole and two-pole collection equipments have been built and tried on commercial trolley-coach routes.
2. *Building for operating speeds of 30 to 40 miles per hour.* At these speeds the trolley coach may be touring to one side of the trolley wires.
3. *Selecting a trolley wire and collector that would prevent excessive wear.* Experience with street-car current collection demanded that the trolley wire last as long for trolley-coach operation as for street-car operation.
4. *Insulating the positive and negative trolley wires.* At both crossovers and turnouts the positive trolley wire crosses the negative trolley wire.
5. *Providing a touring range to permit of curb loading, also the passing of other vehicles in traffic.* The trolley coach should tour over, at least, three traffic lanes.
6. *Automatically selecting the proper path at a turnout point.* This necessitates the selection of either turnout or main line at the will of the operator, and the selection must be made without the operator leaving the coach.

These problems were not solved on the first installation. Our present overhead system is the result of the development of 20 years of experience. The pioneer systems, such as those installed in Staten Island, Philadelphia, Baltimore, Toronto, Windsor, Rochester, and Petersburg, as well as in English cities, were very simple, but they were responsible for many important developments and standards in use today. For example, the advantages possible with four trolley wires providing two-way operation rather than two trolley wires providing "single-track" operation,

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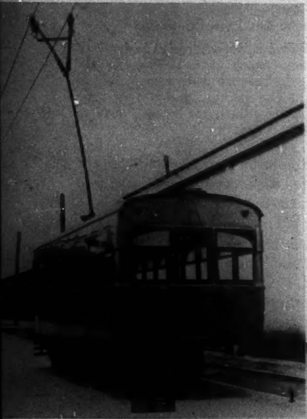
were recognized during the early '20's. Furthermore, considerable additional conductivity was made available for the carrying of electric energy.

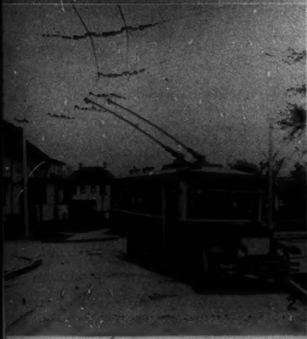
The selection of 2/0 and 3/0 trolley-wire sizes was made on the early installations, because the short life of 1/0 and the additional supporting structure necessary for 4/0 were recognized as economically unsound. As early as 1923 the American Transit Association selected a 24-inch spacing between positive and negative trolley wires and recommended a trolley-wire height of 18 feet above the street. These limitations are standard today. After considerable experimentation with a single-pole collector, two individual poles were finally accepted on the Staten Island property as the standard. The general method of insulating the positive from the negative contact wire, the length of insulation, and the location of the contact wires with respect to the curb line are other features developed 20 years ago which have helped to secure a well-standardized overhead design.

Since 1928, when Salt Lake City installed the first modern 40-passenger transit type of trolley coach, each new installation of trolley coaches has added some new features to the overhead system. These new features have dealt chiefly with the improvement and development of new *devices*, the *dimensional* standards and limitations having remained unchanged. It is an interesting thought that without some of the early standards selected for trolley-coach overhead distribution design, we might have had as many different gauges between positive and negative trolley wire as the railroads had track gauges 75 years ago.

Typical Distribution System

The standard tangent distribution system consists of a pole structure either wood, concrete, or steel, which supports four trolley wires with the poles spaced at approximately 100-foot intervals. The trolley wires are attached to insulated hangers and clamps, and, in addition to the insulation placed in the supporting hangers, a secondary insulation, either porcelain or wood, is inserted in the span wire near the pole. The center line of one pair of trolley wires is usually located 13 feet from the curb line thus permitting the trolley coach to load at the curb and to tour around double-parked vehicles.





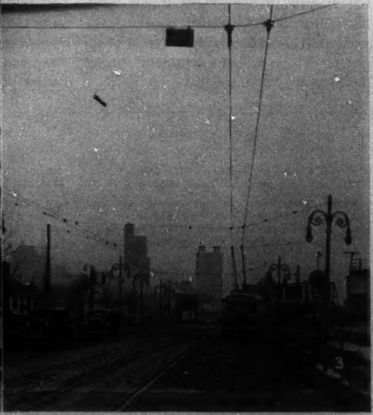


Figure 1. Early single-pole current collector equipped with trolley wheels, 1921

Figure 2. Early two-pole collection equipment, Windsor, Ont., 1922

Figure 3. Standard tangent construction consists of a pole structure, either wood, concrete, or steel, that supports four trolley wires with poles spaced at approximately 100-foot intervals

Development of Collection Equipment

Early collection equipment included a free-swiveling trolley base mounted on top of the coach, a long trolley pole, and a free-swiveling wheel harp for operation under the trolley wire. The trolley base was similar to that installed on street cars, the trolley pole was a lightweight alloy steel pole insulated from the trolley harp, and the harp itself was a wheel collector similar to an ordinary caster. With this combination the trolley coach could tour on either side of a pair of trolley wires, the limitation to the touring range being chiefly the length of the poles. This early assembly of equipment is similar to that employed today except that the wheel collector has been replaced with a shoe collector. Owing to the point bearing of the trolley wheel and to the swiveling action of the device, the wheel collector was subject to many dewirements when passing through frogs and crossovers, and milling action of the spinning wheel on the trolley wire, particularly with the

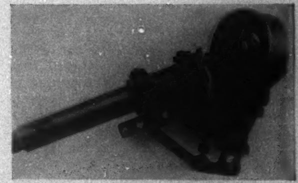


Figure 4. Early collection equipment included a free-swiveling harp equipped with a four-inch trolley wheel

The harp was insulated from the steel trolley pole

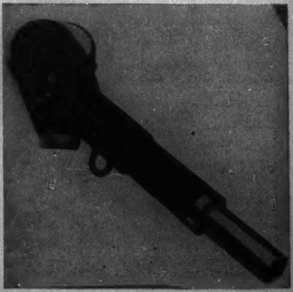


Figure 5. Higher trolley-coach speeds were responsible for the development of the collector shoe

This shoe is all-metallic. The harp is insulated from the trolley pole

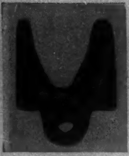


Figure 6. Grooves wore in the early forged-steel and malleable-iron collector shoes

These grooves were injurious to overhead equipment and trolley wire

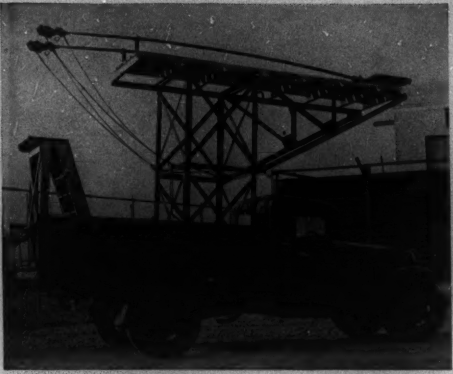


Figure 7. A lubrication truck equipped with two trolley poles and roller type of lubricator for spreading graphite lubricant on the trolley wires

This lubricant was necessary with an all-metallic shoe

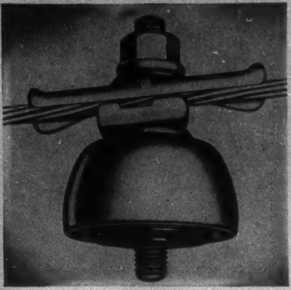


Figure 11. A molded-insulation hanger with adjustable feature for alignment on sloping span wires

trolley coach touring off-center, was quite injurious. These facts were responsible for the development of the shoe.

With the installation of the larger fleets of trolley coaches and the use of higher accelerations and higher speeds, the wheel collector was abandoned in favor of a shoe collector. The history of the development of the shoe collector is interesting. The first shoe collectors were all-metallic, either malleable iron or forged steel. The wearing of a groove in these collectors produced a cutting edge that was injurious to trolley wire, particularly round trolley wire that was supported with a trolley ear encircling the wire. Soft-nosed or bronze-tipped shoes diminished this cutting action but did not prove entirely satisfactory. All of these metallic shoes required lubrication of the contact wires for the prevention of excessive wear to wire and fittings. Usually a graphite lubricant was applied with special roller-type lubricator attached to trolley poles mounted on a line truck or a service truck. Five years ago a collection shoe consisting of a solid piece of carbon permanently embedded in a bronze shoe was installed on a complete system. This was a real step in the right direction, but trolley-wire lubrication was still necessary since the bronze of the shoe continued to rub the wire. The final step was the development of a collecting shoe consisting of a bronze holder in which is clamped a carbon insert that collects electric energy from the trolley wires and operates in a manner similar to a carbon brush on a commutator. This small carbon insert is replaced when it is worn to the allowable limit. Lubrication of trolley wire has been completely eliminated since the carbon insert lubricates and burnishes the trolley wire.

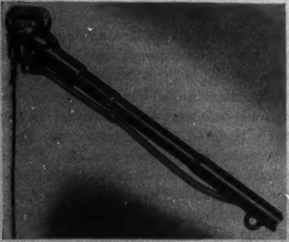


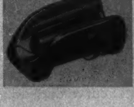
Figure 8. This shoe collector widely used in the United States and England is equipped with a bronze holder in which is clamped a carbon insert

Only the carbon insert contacts the trolley wire

Effect of Shoes on Trolley-Wire Life

When the metallic shoe was first introduced for collection purposes, dewirements diminished, there was less arcing, and collector noise disappeared, but, on the other hand, the cutting effect of the groove worn in the metallic shoe, and the wear on trolley wire and fittings was greatly increased.

This wear was controlled by the substitution of grooved trolley wire for round trolley wire. All fittings were attached to the upper lobe of the grooved trolley wire, the lower lobe being free of attachments, thus presenting a smooth underrun to the collector. At this time it was estimated that the life of a 2/0 grooved trolley wire would be 500,000 bus passes. Actual experience showed figures as low as 300,000 bus passes. At the present time the combination of a *carbon*-insert shoe operating on grooved trolley wire has increased 2/0 grooved-trolley-wire life to figures varying from two million to seven million bus passes. This is equivalent to saying that 2/0 grooved trolley wire might last on the average line from 20 to 70 years. Most installations today are being equipped with 2/0 wire. The 3/0 wire is selected only where conductivity



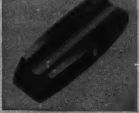


Figure 9 (left). Carbon insert installed in bronze holder

Figure 10 (right). Pregrooved carbon insert

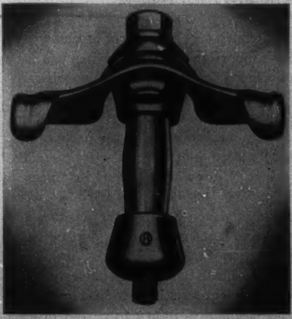
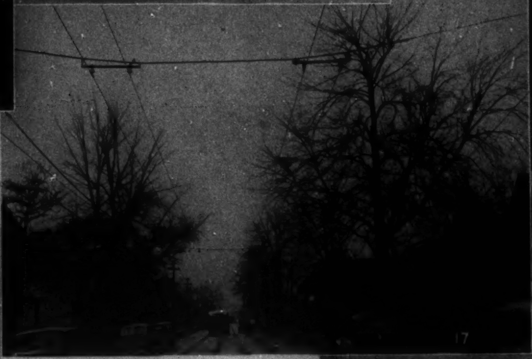
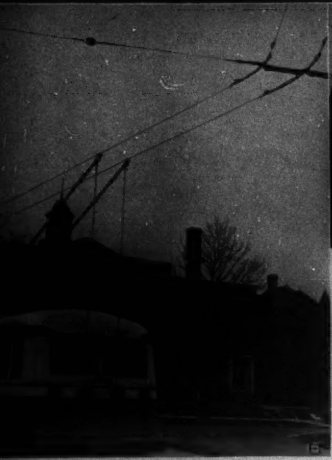


Figure 12. The wood-stick hanger provides a longer leakage path than is possible with most of the hangers equipped with molded insulation





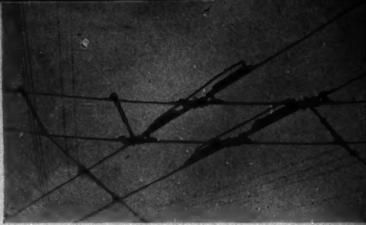


Figure 13. The usual conventional curve designed for street-railway work requires the frequent installation of pull-overs in order to align the trolley wire with the curvature of the track

Figure 14. The segment is the equivalent of a large pull-over; however many 90-degree curves are built with either two 45-degree segments or three 30-degree segments

Figure 15. Segments can be used for trolley-coach overhead since the collection equipment permits of touring either side of the trolley wires

Figure 16. Long radius segments do not restrict speeds

Figure 17. Recent feeder span installations make use of the copper feed span for supporting the trolley wires

Figure 18. A typical crossover showing a location of insulating units in one line; the other line is not insulated

demands. Bronze trolley wire, because of its greater life and ability to hold sufficient tension, so necessary for good operation, is almost universally used.

Trolley-Coach Hanger

A trolley-coach hanger or insulator used for supporting the trolley wires has been developed for this class of two-wire suspension. The hanger carries sufficient insulation in itself to eliminate further secondary insulation, with the exception of that placed in the span wires near the poles. However, in some instances a secondary insulator, usually a wood stick, is placed between the positive and negative hangers in the supporting span. Insulation in the hanger is usually a molded insulation of high dielectric and has sufficient resistivity to heat, moisture, and mechanical injury to permit of long life. Recently several installations have been made with a wood-stick hanger. This type of hanger provides a longer leakage path than is possible with the molded insulation hanger. Hangers are built with adjustability in order to properly align the trolley clamps to prevent interference with the collecting shoes.

Curve Segment

The usual conventional curve designed for street-railway work requires the frequent installation of pull-overs in order to align the trolley wire of the street car with the curvature of the track. This is necessary because of the type of rigid collection equipment normally used on street cars. In the case of the trolley coach, it is not necessary that the vehicle follow beneath the line of trolley wires since the swiveling action of both the trolley base and the harp permit off-center touring. With this equipment it is not imperative that a trolley wire follow any particular curve except that it must be within reach of the collection equipment.

The conventional curve employing numerous pull-overs has been almost entirely replaced by segment construction where the trolley coach does not operate jointly beneath the same trolley wire as the street car. The segment is a large pull-over built to provide an easy means of aligning trolley wires over the path of the coach and, at the same time, to eliminate many fittings and cables formerly used on the conventional street-railway curve. The segment is built with a comparatively large radius and, of course, connects the several chords on the curve. An adjustable feature reduces the total number of segments for a given job to a few. This device does not restrict speeds on curves, inasmuch as it has been designed to handle the maximum speeds possible for the various curves. It has been responsible for the elimination of some unsightly pull-over wires and has contributed to considerably less labor cost during installation.

Feeder Span

The frequency of feeder spans for tapping either positive or negative contact wires to aerial feeders is dependent upon electrical loads, cost of annealed trolley wire occasioned by a trolley wire break and the total cost of feed span installation. As a general rule feed spans for the same polarity are located at 800-foot intervals. This means one negative feed span and one positive feed span in an 800-foot section. Since tapping of the positive trolley wire requires good insulation in the feed span because of the closeness of the negative contact wires, special equipment has been designed for making these taps. The most recent arrangement uses the copper feed span as a supporting span, direct taps being taken from the span to the trolley ear as illustrated in Figure 17.

Special Work for Intersections

At locations where one trolley-coach system crosses another trolley-coach system, or where a trolley-coach system crosses a street-car line, insulated crossovers must be installed. At locations where one trolley-coach line leaves another trolley-coach line, or where a street-car line leaves a trolley-coach line, special turnout equipment is required. In all cases the special equipment includes devices to which a trolley wire is attached. These devices may be for the purpose of crossing two lines or for the purpose of taking one line off another line. In any event, the design of these devices has been made so that the cross section of the runner pieces, metallic or insulated, is approximately equivalent to that of the trolley wire. This uniform cross section of trolley wire and fittings reduces bumping and scrubbing and consequent arcing by the collector and also prevents considerable damage and wear. To the trolley-coach rider, it is important, because it is responsible for very quiet collection operation. Even the trolley-wire splicer is attached to the upper lobe of the trolley wire itself, in such a manner that there is no obstruction to the passage of the collector.

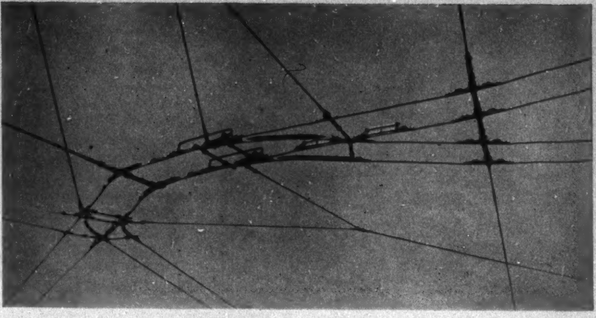


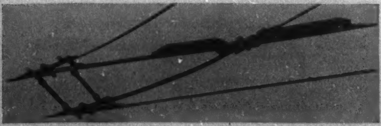
Figure 20. An electrically operated frog installed immediately ahead of a segment

There are many types of crossovers and turnouts, most of which are constructed with standard parts. For instance, in Figures 18 and 20, similar crossover pans have been used, and the same insulation unit occurs repeatedly. The tips for connecting the trolley wire to the devices are the same for the same size of wire. In the case of the turnout, the standard arrangement shown in Figure 19 is equivalent to that used on all turnouts, the frog pan itself being the only piece that is selected for some particular type of operation. In these illustrations the standard insulating unit, standard tips, and crossover pan may be seen. Of course it is understood that the degree of turnout may vary the degree of the crossover pan in either a crossover assembly or turnout assembly.

Insulating Unit for Turnouts and Crossovers

The insulating unit used in all crossover and turnout assemblies must withstand the ultimate mechanical loads of the largest sizes of bronze trolley wire. It must provide adequate insulation during the passage of a collection shoe from positive to negative wires and must withstand the repeated arcing caused by the breaking of electrical loads. Structurally, this unit consists of two phenolic tubes, a tension member, and a compression member. The compression member is constructed of insulation only, while the tension member is reinforced with a steel core, properly insulated at the ends, that enables the assembled unit to withstand all mechanical loads transmitted by overhead trolley wires. The unit provides 12 inches of clear insulation between metallic sections, this distance being adequate to prevent the "carry-over" of the 600-volt arcs.

Figure 19. Typical turnout arrangement where one trolley-coach line joins another trolley-coach line



Fiber insulation formerly used between positive and negative wires is shown in this view. Fiber has now been replaced with a phenolic insulation

Types of Turnouts

Again looking at the standard turnout, a set of ordinary cast trailing frog pans will permit a trolley coach to "trail through" this arrangement from either the main line or the turnout, no additional guiding of the swiveling shoe being necessary. If the direction of operation is reversed, it can be readily seen that further guidance of the shoe is imperative, otherwise the shoe may either enter the straight line or the turnout path. Where the trolley coach enters a frog assembly of this type, the shoe must be guided.

One type of turnout where the shoe is guided to one path only, is equipped with a spring frog similar to a spring track switch. If the frog is set for the turnout at all times, the shoe will enter and take the turnout at all times. With the spring arrangement it is possible to trail from either the main line or the turnout, the same as a rail car trails through the spring track switch.

The movable runner of this frog, as well as all electrically operated frogs, is of the "double-tongue" type. A shoe collector

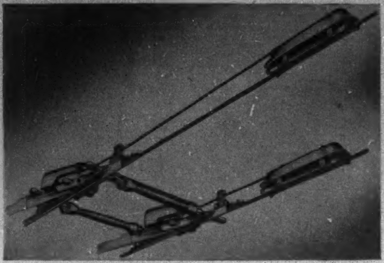

This frog is operated by the two collectors touching the contactors at the same time. In this view one collector is located ahead of the other; therefore, the two contactors could only be simultaneously touched when a trolley coach is turning into a side street. When the trolley coach is taking a turnout, one collector will lead the other collector

Figure 21. Location of contactors on trolley wires in front of Selectric frog

passing through the frog, either over the turnout or over the main line, will travel over one of the runners. Smooth operation of the shoe is accomplished in this way.

There are numerous locations where one trolley-coach line branches away from another line. At these locations it is necessary for the operator to select either the main line route or the turnout route. At these locations electrically operated frogs are usually installed, although in some minor cases, frogs operated with pull ropes have been used.

The electrically operated frog is of two types, the "power-on—power-off" and the Selectric type. In the former type the path at the point of turnout is selected by the operator of the coach by either coasting through the device (power-off) or by taking power (power-on) to actuate the solenoid-operated frog. In this assembly the frog pan is insulated from the trolley wire. It is, however, electrically connected to the trolley wire through a solenoid which operates the frog runners. If a coach passes through this section with the "power off," the runner remains in the main-line position. If, however, the coach passes through this section with the "power on," current passing through the solenoid actuates the frog runner and sets it to the turnout position. Many modifications of this type of frog are in use; however in principle they are similar to this one.

The other type of electric frog depends upon the angularity of the trolley-coach body with the trolley-coach wires. The electrically operated frog used with this combination is also equipped with only one solenoid; however, it is also equipped with a mechanical reset. The solenoid actuates the frog runner, and after a collector passes through the frog, it strikes a trigger and resets the frog to its original position.

Two contactors, one on each trolley wire, are located ahead of the frog. If the two contactors are placed abreast, and a trolley coach is continuing over the main line, the collector shoes will be abreast and will strike the two contactors simultaneously. Since these contactors are a part of a circuit to the coil, the frog runner is actuated as the shoes pass beneath them. After the collection shoe has passed through the frog, it strikes a mechanical resetting device and throws the frog runner back to its initial position, which in this case would be for the turnout. If the coach is to take the turnout, one trolley shoe will lead the other; therefore the shoes cannot strike the two contactors simultaneously and complete a circuit to the solenoid. If the trolley coach is taking a turnout there will be no movement of the frog runner, and since the reset position of the runner is for the turnout, the action is positive.

Other variations of this type of electric frog are in use, the variations being due chiefly to the locations of contactors. In addition to these variations, frequent installations of one half of an electric frog are used where a street-car line either enters or leaves the trolley-coach overhead system.

At the present time, an intersection assembly, turnout or crossover, is ordered and shipped as a unit. Previously, the transportation company selected and erected a multitude of small devices, but the confusion caused by this procedure resulted in an effort by all manufacturers to simplify the selection of the proper assemblies for an installation. Unit assemblies are easily identified.

Research an Important Factor

The design and development of trolley-coach overhead required many laboratory and field tests of materials and assemblies. Impregnated wood beams were first used in crossover assemblies to furnish both mechanical strength and insulation between positive and negative wires. Partially because of bulkiness and weight, but chiefly because of occasional mechanical failures, the wood beam was replaced with a fiber beam. Fiber furnished sufficient insulation for the 600-volt system and provided ample strength, but fiber has one inherent feature that was responsible for the discontinuance of its use. Fiber beams warp. As a consequence, a straight, smooth underrun was difficult to secure. Variations in moisture even produced warping in stock bins before the devices were shipped. The present tubular, phenolic members of an insulating unit do not warp, have good mechanical strength, and furnish excellent insulating qualities over a period of years. Many service tests were necessary during the development of this insulation.

Life tests on the insulation included repeated collector passages over the runners to determine the deteriorating effect of the arc. An insulating unit is tested with a shoe passing under it at speeds corresponding to the speeds of trolley-coach operation in the street. The life of the unit is determined for various values of current at 600 volts. These tests are responsible for the selection of the general design as well as the selection of materials that will withstand constant arcing. It is to be remembered that the auxiliary load of a trolley coach for lights, heaters, and compressor will reach 70 amperes, and this load must be broken at an insulator even when the traction load is cut off.

The development of the current collection equipment has continued for many years. The first carbon inserts used in

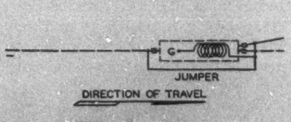


Figure 22. Wiring diagram of a "power-on—power-off" electric frog

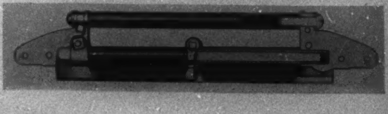


Figure 23. Cut section of the phenolic insulating unit used in crossover and frog assemblies

the present shoe wore out in less than 10 miles, the next carbon inserts in less than 30 miles, and the inserts finally accepted and offered for general use would not last more than 500 miles. The average mileage today for summer and winter, dry weather and wet weather, is considerably over 1,000 miles, and it required four years to secure this mileage. Because of the research conducted by the London Passenger Transport, the present development of the carbon-insert collector was no doubt hastened. Similar experiments were carried out in London and in this country. The exchange of data was very helpful in expediting the production of a satisfactory collector.

The usual problems with lightning and radio interference were encountered, and satisfactory modifications and improvements were developed to provide the degree of reliability demanded for city transportation service. Today, radio complaints are few and there is no record of damage to the traction motors due to lightning.

Reduction in Weight

The weight of the overhead system has always been a problem. A one-pound weight in the overhead system produces from five to eight pounds side load on the supporting structure, either pole or building. The reduction of weight in the overhead system reduces the side loads on the structures and, as a consequence, the cost of the system. Substituting 2/0 grooved trolley wire for 3/0 grooved trolley wire has reduced the weight 20 per cent. The weight of overhead fittings has been reduced 35 per cent since 1934. This weight reduction has permitted the use of many street-car poles that formerly supported two trolley wires. Now they support four trolley wires.

The Picture of Trolley-Coach Operation Today

In addition to the study and development of fittings for overhead design, each new trolley-coach installation has required a field study, a study of the application of the materials. Today, trolley coaches are operating in every climate, almost in every country. They operate at speeds up to 45 miles per hour, accelerate at four miles per hour per second, collect currents as high as 400 amperes, climb 13 per cent grades, run through subways, through congested streets, and over country highways. They do this quietly, swiftly, efficiently, under the overhead distribution system just described.

There are 3,300 trolley coaches now operating in the United States and Canada, 95 per cent of which have been installed within the last 10 years. These coaches, operating over 1,100 miles of route, totaled 100 million coach miles during 1940, and each coach averaged over 1,000 miles for each trolley-pole dewirement.

This is equivalent to saying the trolley coach can run at least five days without a dewirement, a performance that is a measure of the reliability of modern trolley-coach overhead.

The Electrical Strength of Nitrogen and Freon Under Pressure

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Synopsis: Results are given of an investigation of the electric strength of nitrogen, of dichlorodifluoromethane (Freon F-12), and of mixtures of these gases. Sparking voltages are presented as measured between spherical electrodes of brass and aluminum and between pointed electrodes of brass, at various spacings, and in gas at pressures ranging from one to several atmospheres. All measurements are for 60-cycle applied voltage. Dichlorodifluoromethane is found to withstand much higher voltages than either air or nitrogen; this advantage is more marked between points than between spheres, which suggests its use in certain types of insulation applications. A small percentage of dichlorodifluoromethane gas in nitrogen produces an anomalously large rise in the electric strength of the gas, indicating practical advantages of such mixtures.

PREVIOUS studies of the electric strength of air under pressure have been reported by the authors.[1,2] The present paper presents data from a continuation of this work for the primary purpose of studying the electric strength of the gas dichlorodifluoromethane, CCl_2F_2, commonly known as Freon F-12. This gas is readily available as a refrigerant and is known to have unusually high electric strength. It was studied both alone and mixed with nitrogen, and to complete the series of tests the electric strength of nitrogen was also determined.

Freon was obtained in liquid form, in a pressure cylinder, and gas from the upper part of the cylinder was released into the test chamber as desired. Nitrogen gas of commercial purity was used; it is guaranteed 95 per cent pure but is believed to be purer than the guaranteed value. It is supplied under pressure, and was released into the test chamber when needed. No purification of either gas was attempted, for it was believed that results obtained with gas of commercial purity would be more significant from an engineering point of view than results obtained with chemically pure gases. In the present investigation no irregularities or inconsistencies of data could be traced to the presence of impurities in the gases supplied.

The apparatus used is described and pictured in a previous publication.[2] The methods of measurement and technique employed in the study of Freon were the same as in the previous work with air, except as the use of mixed gases required special methods.

In order to obtain mixtures of gases of known composition for test purposes, it was necessary to evacuate the test chamber before admitting the gas or gases in which the test was to be performed. Pressure within the chamber was reduced by means of a vacuum pump to a value estimated at one millimeter of mercury before admitting nitrogen or Freon. By this means practically all of the residual gases were removed. This was particularly necessary for the purpose of removing all traces of Freon gas before testing in an atmosphere supposed to contain no Freon. The presence of an extremely small amount of Freon in nitrogen or air was found to be important in determining dielectric strength, sometimes increasing the sparking voltage by fifty per cent or more,

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although the amount of Freon could hardly have been more than a fraction of a per cent. The sparking voltage in such cases was highly erratic and undependable. Similar results were obtained when a trace of carbon tetrachloride was present in the test chamber.

After a series of tests with Freon, it was found best for the test chamber to be evacuated and left for several hours or days. Nitrogen was then admitted to the chamber, and it was again evacuated. This process removed all indication of the presence of Freon in subsequent tests.

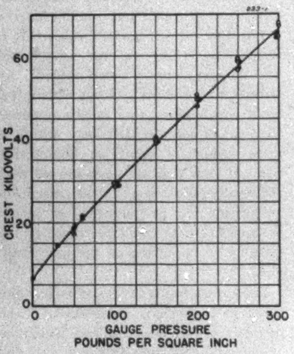

Figure 1. Comparison of sparking voltages in air and nitrogen

Spherical electrodes
Length of spark gap: 0.050 inch
○ Experimental points in air
● Experimental points in nitrogen

In order to obtain satisfactory mixing of gases when Freon and nitrogen were used together, it was found necessary to stir the gases with an electric fan placed within the test chamber. Natural mixing by diffusion did not take place in the course of several hours. When the fan was not used, the heavier Freon went to the bottom of the test chamber and remained partially unmixed with the nitrogen. Results without use of the fan were inconsistent and meaningless. Less than five minutes of operation of the fan was adequate to give thorough mixture of the gases, and results obtained with its use were consistent and could be repeated from day to day.

For test of sparking between spherical electrodes in gas mixtures containing Freon, aluminum electrodes were used in most cases. Aluminum was used because of its greater resistance to corrosive action under such circumstances. Brass was less resistant than aluminum, and steel was less resistant than brass. It is presumably not Freon itself that is harmful to the metal electrodes, for it is reported—in publications relating to its chemical properties—to be noncorrosive, and it is used satisfactorily for refrigeration. Electric discharge in Freon, however, is reported to produce chlorine and other corrosive products of decomposition. It is apparent from the present investigation that the products of electrical discharge in Freon are distinctly corrosive. Moreover, products of electric discharge in a mixture of Freon and air are very considerably more corrosive than those that result from Freon alone or in mixtures of Freon and nitrogen. Corrosion of electrodes in mixtures of Freon and nitrogen was about as severe as in pure Freon, and very much less than in mixtures of Freon and air.

Sparking in nitrogen was remarkably free from corrosion of any kind, and hundreds of sparks would barely tarnish the polished surface of either brass or aluminum spheres. Pointed brass electrodes lost none of their original sharpness after forty or fifty sparks in nitrogen, whereas they were noticeably rounded and measurably shortened in length by similar sparking in either air or Freon.

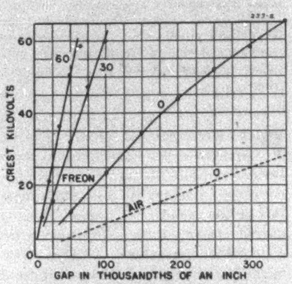

Figure 2. Sparking voltages in Freon

Spherical electrodes
Gauge pressure in pounds per square inch as noted on curves

Results in Nitrogen

In general, sparking voltages between spheres in nitrogen are the same as between spheres in air. This conclusion was checked carefully with an 0.050-inch gap at pressures from 1 to 21 atmospheres; sparking voltages in nitrogen could not be distinguished from those in air (see Figure 1).

A careful study of sparking between spheres at various spacings and pressures had been made in air,[2] and because of the similarity of results this was not repeated in nitrogen.

Despite the general agreement observed between sparking between spheres in air and in nitrogen, there were certain differences in detail. It has been mentioned[1,2] that in air there was a tendency for the sparking voltage to rise slightly as a number of measurements were made in rapid succession. No such result was noticed in nitrogen. It has also been mentioned that a spark would usually result in air if voltage was held for a period of several minutes at a value as much as five to seven per cent below the usual sparking value. This effect did not appear in nitrogen. The sparking values given in this report for nitrogen are average values. Most of the individual sparking values for pure nitrogen lie within one per cent of the average value, the most extreme variations being about five per cent.

It should be emphasized that a slight trace of Freon in the nitrogen would cause very erratic results, but that clean nitrogen was quite consistent.

Results in Freon

Sparking in Freon was investigated at pressures of from one atmosphere to about six atmospheres, with gaps varying from 10 mils to 250 mils (0.010 to 0.250 inch) between both brass and aluminum spheres. Sparking between points was also studied (see following). The highest pressure possible at room temperature was about six atmospheres, at which pressure the gas is in equilibrium with liquid Freon. (The vapor pressure of Freon at 70 degrees Fahrenheit is 84.82 pounds per square inch absolute, 70.12 pounds per square inch gauge[3]).

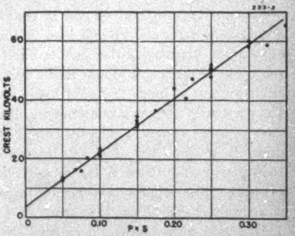

Figure 3. Sparking voltages in Freon

Spherical electrodes
Points are experimental; line is computed from $V = 183PS + 4.0$ kilovolts
P, absolute pressure in atmospheres
S, spark-gap length in inches

Freon was found to have a sparking voltage between spheres that was, for all pressures investigated, between 2 and $2^{1}/_{2}$ times as great as the sparking voltage in nitrogen or air at the same spacing and pressure (see Figure 2).

The nature of the spark in Freon was not noticeably different from the sparks observed in air and nitrogen, except in color. Sparks in Freon are intensely blue, in nitrogen they are purple, and in air white or slightly yellow. The intensity of sparks in the different gases, however, appears to be much the same when due allowance is made for the amount of current flowing.

Values of sparking voltage obtained in Freon were fairly consistent, although a variation of two or three per cent from the average value was not uncommon. Variations of as much as ten per cent were occasionally encountered, usually below the average sparking voltage rather than above. No "trends" of voltage values, as found in air,[2] were discovered in Freon, and there were no sparks at voltages radically different from the average value.

In general, sparking voltage between spheres in Freon was found to be proportional to the length of gap, and proportional to the pressure of the Freon gas. To be more precise, the voltage-spacing and voltage-pressure relations were found to be linear, but not exactly proportional, taking the same form as Paschen's law for sparking in air.

The breakdown voltage of Freon as determined in the present investigation can be expressed by the formula

$$V=183PS+4.0$$

where V is sparking voltage in kilovolts, P is pressure in atmospheres, and S is spacing in inches. It will be seen that this equation gives voltage values for sparking in Freon between 2.4 and 2.5 times the values given by Paschen's law for air.[2]

The preceding formula appears to be reasonably accurate over the range of conditions of test. The values of sparking voltage obtained by experiment, each an average of five to ten individual voltage readings, are all within ten per cent of the value given by the formula. The experimental range of pressure was from one to five atmospheres, and the range of spark length was from 10 to 350 mils, giving a range of the product PS from 0.05 to 0.35 (atmosphere-inch). This is shown in Figure 3. This formula was derived from the authors' results alone. However, it was found to give good agreement with the sparking voltages measured between flat electrodes in Freon by Trump, Safford, and Cloud (see Figures 3 and 4 of reference 9), for pressures up to about four atmospheres (60 pounds per square inch absolute). As the vapor-pressure of Freon is approached (six atmospheres at room temperature) the experimental sparking voltages are lower than would be expected from the formula. Agreement with the work of Trump, Safford, and Cloud appears to extend the range of applicability of the formula to direct voltage as well as alternating, to gap lengths as great as 0.7-inch, and to values of the product PS as great as 2.0 (atmosphere-inches).

Results in Freon-Nitrogen Mixtures

The electric strength of mixtures of Freon and nitrogen gas was measured under a variety of conditions to determine the effect of

(*a*) Dilution of Freon by nitrogen.

(*b*) Change of pressure.

(*c*) Change of spark-gap length.

Results, which will be discussed below, were intermediate between those obtained in pure Freon and those obtained in pure nitrogen. This was the outcome that was to be anticipated. One very interesting relation, however, was discovered. It was found that a very small amount of Freon in nitrogen has a disproportionately large effect in raising the electric strength; this is discussed below.

Considerable difficulty was at first experienced in work with mixed gases, and results could not be repeated from time to time until an electric fan was used to insure thorough mixing of the gases within the test chamber. When the fan was used, however, the difficulty was entirely overcome, and sparking voltages were thereafter obtained with about the same degree of consistency as in pure Freon: that is, individual readings of sparking voltage commonly varied two or three per cent above and below the average for a given condition, while a variation of as much as ten per cent was very unusual.

Mixtures of Freon and nitrogen that were given most attention contained 67 per cent Freon, 50 per cent Freon, 33 per cent Freon, 25 per cent Freon, and 5 per cent Freon by volume. Most of the work was done with a spark-gap length of 50 mils (see Figure 4). To give assurance that the normal relation between the sparking voltage and gap length exists in

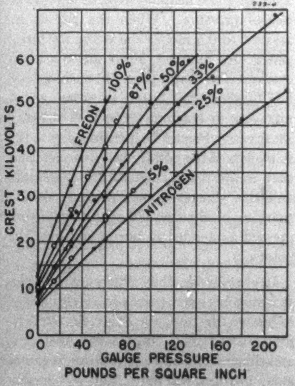

Figure 4. Sparking voltages in mixtures of Freon and nitrogen

Spherical electrodes
Length of spark gap: 0.050 inch
Per cent of Freon by volume as noted on curves

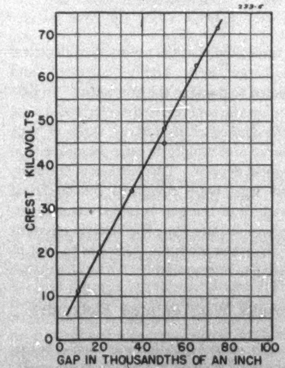

Figure 5. Sparking voltages in a mixture of 50 per cent Freon and 50 per cent nitrogen

Spherical electrodes
Pressure seven atmospheres (90 pounds per square inch gauge)

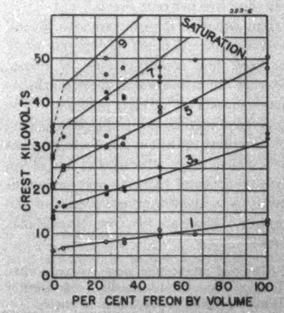

Figure 6. Sparking voltages in mixtures of Freon and nitrogen

Spherical electrodes
Length of spark gap: 0.050 inch
Pressure in atmospheres as noted on curves
Points are experimental; lines are computed from $V=(88PS+1.9)(1+1.08F)$ kv

mixed gases, the gap length was varied from 10 to 75 mils in a mixture of 50 per cent Freon and 50 per cent nitrogen with the results shown in Figure 5. As with pure gases, the voltage-distance relation is linear, and experimental data for this mixture of 50 per cent Freon and 50 per cent nitrogen can be represented by the formula

$$V = 132PS + 2.5 \text{ kv}$$

(All experimental points lie within 10 per cent of this expression.)

This suggests the possibility of finding a simple equation to represent data for all Freon-nitrogen mixtures at all pressures and spacings. For this purpose the data of Figures 4 and 5 were replotted as Figure 6. In this form it is evident that the relation between the amount of Freon present and the sparking voltage is linear when the concentration of Freon is greater than five per cent. When the concentration of Freon is less than five per cent a small amount of Freon produces a disproportionately large increase in the sparking voltage, as shown in detail in Figure 6 at a pressure of three atmospheres. The linear relation shown in Figure 6 leads to the following equation for sparking voltage in Freon-nitrogen mixtures in which the amount of Freon is greater than five per cent:

$$V = (88PS + 1.9)(1 + 1.08F)$$

where V is kilovolts, P is pressure in atmospheres, S is spacing in inches, and F is the fraction of Freon by volume, in the gas. (For pure Freon $F = 1.0$; for half Freon, half nitrogen $F = 0.5$; note that for pure nitrogen it is *not* correct to let $F = 0$ as the formula does not apply for less than five per cent Freon.)

This equation is proposed with several reservations. As noted, it does not apply for very low concentrations of Freon. Another limitation is that the formula becomes inaccurate at high pressure. The agreement with experimental data is good for pressures up to five atmospheres. At pressures of seven atmospheres and more, the experimental sparking voltage is uniformly less than that predicted by the formula. There are two obvious explanations:

1. It is at about this same pressure that variation from a straight-line relationship becomes evident in pure air or nitrogen.
2. The discrepancies become more marked as the partial pressure of Freon in the gas under test approaches the vapor pressure of Freon.

A condition of saturation is approached when both pressure and concentration of Freon are high, corresponding to the upper right-hand corner of the chart. As the vapor pressure is approached the intermolecular forces become great and the Freon no longer approximates a perfect gas.

Finally, the formula given for mixtures of Freon and nitrogen is based on a limited amount of data. Since there are three independent variables in the equation, it would be an extremely lengthy procedure to determine the voltage for all possible combinations. The data at hand were obtained by recording slightly over a thousand individual sparking voltages, however, and appear adequate to give strong support to the above formula for pressures greater than one atmosphere, for spacings from a few mils to a few hundred mils, and for all concentrations of Freon greater than five per cent.

Some work was done with mixtures of air and Freon, but was discontinued when it was found that the products of electrical discharge were highly corrosive and tended to damage the apparatus. In particular, the spheres used as electrodes were tarnished after as few as three or four sparks and rapidly became coated with a grayish deposit. This corrosion, however, had very little effect on the electrical strength of the gap between the spheres. Indications are that the electric strength of air-Freon mixtures is similar to that of nitrogen-Freon mixtures.

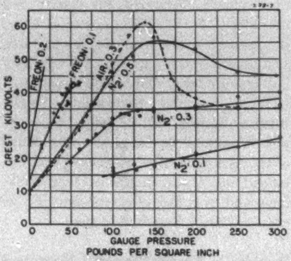

Figure 7. Sparking voltages in nitrogen, Freon, and air

Pointed electrodes

Gas tested, and spark-gap length in inches, as noted on curves

Results With Pointed Electrodes

A short investigation was carried out to determine some of the salient characteristics of sparking in air, nitrogen, and Freon, between electrodes with sharp points.

Sparking in air between points was discussed at some length in a previous paper,[1] and present measurements agree, where comparable, with the results which were then obtained. Experimental procedure is difficult because the nature of the results depends to a large extent on the degree of sharpness of the points, particularly for short gaps. In general, however, as pressure is raised, the sparking voltage reaches a maximum; this maximum occurs at 8 to 12 atmospheres pressure. When pressure is further increased, the sparking voltage becomes less—sometimes very slightly less, sometimes by as much as 50 per cent. The large decline of voltage with increasing pressure is obtained with very sharp points, while with blunt points the dip becomes less marked, and there may be no dip whatever if the points are either quite blunt or quite close together. The action is apparently dependent on whether or not corona precedes sparking; observation of corona has been made possible by the windows installed in the present test chamber.

Sparking in nitrogen between points was investigated for three lengths of gap. It was found that the sparking voltage in nitrogen is considerably lower than that in air for the lengths of gap studied. Curves of sparking voltage as a function of pressure are shown in Figure 7. No direct comparison with the strength of air is possible because of the different shape of the curves, but it is seen from the figure that a 0.3-inch gap in air will withstand about as much voltage as a 0.5-inch gap in nitrogen. There is a decided difference in the shape of the curves; the curve for the 0.3-inch gap in nitrogen is practically flat for pressures above ten atmospheres, while the corresponding curve for air has a hump in the neighborhood of ten atmospheres pressure. When the gap is increased to 0.5 inches the curve for nitrogen is also humped, although to a lesser extent. Very short gaps (0.1-inch or less) fail to show any hump in either air or nitrogen, at least for points of ordinary sharpness. It may be mentioned that the sharpness of the points used was such that they would readily scratch the fingernail, and this sharpness was retained during sparking in nitrogen but sparking in compressed air tended to burn and blunt the points.

It is probable that difference between sparking between points in nitrogen and in air results from the high electron affinity of oxygen. Oxygen will allow the attachment of electrons to form negative ions; nitrogen will not.[8] This will greatly affect the mobility of space charge in the gas, and since sparking voltage between points is largely influenced by space charge, it is natural that nitrogen and air behave differently between points although they behave alike between spheres.

The voltage required to spark between points in Freon is considerably higher than for equal distances in air. Curves for sparking in Freon are shown in Figure 7. Again a general comparison is practically impossible, but certain particular values may be compared in air and Freon.

The voltage required to spark 0.1 inch between points in Freon is for all pressures (from normal atmospheric pressure to the vapor pressure of Freon) greater than the voltage required to spark 0.3 inch in air or 0.5 inch in nitrogen. Over most of the pressure range the voltage required for the shorter gap in Freon is very considerably greater than for the longer gaps in air and nitrogen. It was found that a gap of 0.2 inch in Freon will support voltage comparable to published values for a gap of 30 to 50 millimeters (1.2 to 2.0 inches) in nitrogen.[7]

Since this paper was submitted, G. C. Nonken has published[10] sparking voltages for longer gaps in Freon. His curves for sparking between points (square rods) show the typical hump. A hump is just beginning to appear in the curve of Figure 7 of this paper at the highest pressure obtainable in pure Freon, and with longer gaps it appears at lower pressure. At gap lengths of six and eight centimeters this maximum sparking voltage is at almost atmospheric pressure, and voltage declines as pressure is raised.

At atmospheric pressure comparison may be made between results obtained in Freon and the AIEE standard needle-gap sparking voltages for air, as shown in Table I.

Table I

Voltage Kv Crest	Length of Spark (Inches)	
	In Air (AIEE Standard)	In Freon (Experimental)
14.3	0.47	0.10
24.0	0.82	0.20

It will be seen that in general the increase of electric strength gained by the use of Freon is greater with pointed electrodes than with flat or spherical electrodes. The apparent reason is that with pointed electrodes, sparking is preceded by corona discharge, while there is no corona between spheres. The conclusion is that formation of corona is greatly impeded by Freon gas.

One of the more important and useful aspects of the behavior of Freon is the following. The maximum sparking voltage between points in Freon is greater than the sparking voltage of the same point-gap in air or nitrogen at any practical pressure of air or nitrogen. With a gap of 0.1 inch, for example, between points, Freon will withstand about 40 kilovolts at 50 to 60 pounds gauge pressure. According to data given by H. J. Ryan[8] a gap of the same length in air will not support more than 35 kilovolts even though the pressure is raised to 1,500 pounds. With longer gaps the advantage of Freon appears to be even greater.

This advantage is peculiar to pointed electrodes. In nitrogen or air the sparking voltage between points reaches a maximum at a pressure of a few atmospheres and thereafter decreases with increasing pressure, or at the most, rises very gradually. When the gap in which sparking occurs is between smooth surfaces—between spherical electrodes, for example—the maximum strength of Freon may be equaled and exceeded in either air or nitrogen by sufficiently increasing the pressure but this is not possible if the spark occurs between points.

These facts appear to make Freon potentially more useful to prevent electric discharge from points or projections than to prevent sparking or breakdown in a substantially uniform electric field. On the other hand, it must be remembered that corona or other electric discharge must not ordinarily be allowed to take place in Freon, because the decomposition products are corrosive and somewhat poisonous.

Conclusions

1. The electric strength of nitrogen between smooth electrodes is almost exactly equal to that of air.

2. Sparking voltage between sharp points is higher in air than in nitrogen. See Figure 7.

3. Freon gas (dichlorodifluoromethane) between smooth electrodes will withstand about two and a half times as much voltage as air or nitrogen at the same pressure, but it cannot be used at pressures above about 70 pounds per square inch, gauge, as that is its approximate vapor pressure at ordinary temperatures. See Figures 2 and 3.

4. Mixtures of Freon and nitrogen are intermediate in characteristics between the two gases used alone. See Figures 4 to 6. The most interesting characteristic of mixtures is the large increase in electric strength produced by a very small amount of Freon in nitrogen. See Figure 6.

5. Freon gas between pointed electrodes increases the strength of the gap very greatly, the sparking voltage being found in some cases to be of the order of magnitude of four times that in nitrogen. The maximum sparking voltage in Freon is greater than the greatest sparking voltage that can be attained in air or nitrogen at any practicable pressure.

6. The greatest advantage in the utilization of Freon between smooth surfaces is where the pressure of gas that may be used is limited by mechanical considerations. For most purposes, nitrogen at 300 pounds per square inch pressure is superior to Freon, for it has as great an electric strength, is more consistent in behavior, and does not become corrosive in the presence of electric discharge. But if the gas pressure is limited by mechanical design to 150 pounds per square inch, or less, the possible use of Freon should be carefully considered.

7. The possibility of adding a small percentage of Freon to nitrogen, and thereby increasing its electric strength by 15 to 25 per cent appears to be a practical consideration.

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Lightning Investigation on Wallenpaupack-Siegfried 220-Kv Line of Pennsylvania Power and Light Company

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THE Wallenpaupack-Siegfried line, the pioneer 220-kv circuit in the eastern United States, was completed in 1926.[1] Little information on lightning, and most of it incorrect, existed at the time, and the design chosen proved inadequate to cope with the severe lightning conditions encountered. Partly because of this fact an investigation was started that year by the General Electric Company, the Electric Bond and Share Company, and the Pennsylvania Power and Light Company, to learn the facts about natural lightning and means for protection against its disastrous effects. This investigation has been continued for 15 consecutive years.[2–8]

During 1926 the prevailing theory of lightning involved a bound charge on the earth due to cloud field, the sudden release of this charge, and generation of steep wave-front and high-voltage surges.[8] Because of this general conception overhead ground wires were not considered worth their cost, and tower-footing grounding was seldom talked of. The only investigational work up to this time had been carried out in laboratories, and was based on conjectured lightning strokes. Knowledge of the characteristics of natural lightning was very meager. By 1930, the direct stroke theory[10,11] was pretty well proved, and data on the remarkable effectiveness of overhead ground wires and buried tower footing grounding cables were being accumulated.

The lightning investigation which was in very large part conducted on the Wallenpaupack-Siegfried line had several closely interrelated aspects, namely:

1. Increasing insulation strength of line insulators and terminal equipment against flashover.
2. Spillway gap performance at terminals of line.
3. Dynamic current measurements at times of faults and estimation of fault locations.
4. Addition of means for shielding against lightning.
5. Lightning-arrester operation and performance.
6. Lowering structure-footing resistance by adding buried metallic cable.
7. Collection of detailed operating records relating to time of occurrence of flashovers and tripouts and to line construction in use.
8. (*a*) Field measurements at many locations along the line of magnitude, polarity, and wave front of surge voltages appearing on line conductors.

   (*b*) Locations of structures hit by lightning and polarity of stroke current.

   (*c*) Magnitudes and polarity of lightning currents in overhead ground wires, lightning rods, tower-structure members, and buried grounding cables.

   (*d*) Instantaneous and integrated measurements of atmospheric voltage gradients and field strength, and induced voltages on aerial conductors.
9. Field measurements of lightning voltage and current wave shapes at attended laboratories located along the line, and which were equipped with cathode-ray oscillographs and surge-voltage recorders or rotating prism cameras.

Table I lists the principal measuring devices used. Since data obtained from these various sources usually have been from 5 to 15 years duration, and since additions and improvements to the line have been made in steps, and since careful analysis and detailed reporting of data have been made, a wealth of unique information exists, reliable in general to within 15 per cent.

The principal measuring devices used in large quantities in the field included:

1. The surge voltage recorder,[12,13] a clock-driven Lichtenberg figure camera used in conjunction with insulator string voltage dividers to indicate polarity, magnitude, and wave characteristics of lightning voltages on line conductors, and voltage-measuring antennas.
2. A simpler instrument without clock called a lightning-stroke recorder[14] was later used in large numbers to indicate surge polarity and location of strokes to structures.
3. A superior device using small magnetizable links called a surge crest ammeter[15,16] has given a wealth of information on lightning current magnitudes and polarity. Thousands of magnetic links have been employed on overhead ground wires, lightning rods, structure members, buried grounding cables, and lightning-arrester leads.
4. Other devices, such as the lightning severity meter and field intensity recorder. In addition, field laboratories[4,17,18] have been operated adjacent to the line during 1928 and 1929 near Wallenpaupack, during 1930 42 miles south of Wallenpaupack, and during 1939, 1940, and 1941, 12 miles south of Wallenpaupack on High Knob Mountain.

Table II lists magnitudes or range in value of various quantities measured on this line.

Table I. Principal Measuring or Counting Devices Used on or Adjacent to Wallenpaupack-Siegfried Line

Name	Purpose and Brief Description	Max. No. Used in Any One Year
Surge-voltage recorder	Lichtenberg figure camera with clock-driven film. For measuring polarity and magnitude of surge voltages on line conductors and elevated antennas	42
Cathode-ray oscillograph	Electron beam in vacuum chamber recording time-voltage graph on photographic film	2
Lightning-stroke recorder	Small and inexpensive surge-voltage recorder using stationary film	378
Field-intensity meter and rate of change of field meter	Light-beam recording on moving film, for recording atmospheric electric-field intensity, or rate of change of field	3 1
Lightning-severity meter	Modified Kodak recording on photographic film an integrated measure of light from neon lamp energized from antenna and recording atmospheric voltage. Not all used adjacent to Wallenpaupack-Siegfried Line	9
Surge indicator (flashover indicator)	Semaphore device with frangible link exploded by voltage drop across tower bridge, such as occasioned by insulator flashover. Exposed target easily visible to patrolman on ground	1,062
Surge-crest ammeter	Small laminated alloy steel cartridge held by wooden bracket in magnetic field surrounding conductor carrying lightning-surge current	1,000 Approx.
Graphic ammeter high-speed (Hall) recorder, or magnetic-oscillograph element	For recording dynamic current or voltage at line terminals at times of line faults	29

Paper 42-74, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 10, 1941; made available for printing December 19, 1941.

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The success of this investigation has been due to the careful correlation between records of operating experience and the research measurements. One essential part of this investigation has been detailed information on insulator flashovers. During the fall of 1927 it became apparent that flashed insulators were not being properly recognized and reported, and so during 1928, 1929, and part of 1930 tower climbing patrols were made after every storm at every tower. In 1930 the surge indicator[4,14] had been invented. This was a device showing a large target to indicate insulator assemblies which had been subjected to high voltages. The surge indicator permitted overhead patrolling to be reduced to reasonable amounts and yet enabled practically all of the flashed insulator assemblies to be promptly found.

Table II. Maximum Magnitude or Range in Value of Lightning-Research Measurements Obtained on or Adjacent to Wallenpaupack–Siegfried Line

Potentials due to lightning	
Steel tower lines, conductors to ground	3,000 kv
Across 40 to 50 feet of vertical length of tower	50 kv approx.
Across 5 feet of earth near tower footing	Greater than 50 kv
Potentials due to switching	4.9 × normal crest line to neutral voltage
Attenuation of voltage surges	Proportional to ϵ^{2}
2,000-kv surge	to 1/2 value in 3.1 miles
1,000-kv surge	to 1/2 value in 6.2 miles
500-kv surge	to 1/2 value in 12.5 miles
Traveling waves due to lightning	
At end of line	
Time of crest	1 to 80 microseconds
Time to 1/2 value on tail	4 to 160 microseconds
At middle of line	
Time to crest	0.1 to 15 microseconds
Time to 1/2 value on tail	0.3 to 50 microseconds
Direct lightning stroke to line	$i = 300\epsilon^{0.8t}$ i = amperes t = microseconds
(Calculated from voltage oscillogram)	
1 microsecond	Current, 700 amperes Rate of rise, 600 amperes per microsecond
5 microseconds	Current 16,000 amperes Rate of rise, 13,000 amperes per microsecond
8 microseconds	Current 200,000 amperes
Atmospheric potential gradient	
Near earth at time of lightning strokes	Positive 180 kv per meter Negative 270 kv per meter
Current due to lightning stroke	
(By surge-crest ammeter measurements)	
In tower structure*	Up to 140,000 amperes (approx.)
Probable stroke current**	Up to 218,000 amperes (approx.)
Polarity negative (neglecting trace records and very small strokes)	99 per cent

*Two other next highest records 102,000 and 108,800 amperes.

**Summation of 5 adjacent and associated records of 20,500, 57,700, 103,800, 14,800, and 16,500 amperes.

Another equally essential result of the investigation has been the obtaining of complete information on individual line tripouts, including time, relay operation, fault location, estimates from magnetic or cathode-ray oscillographs, actual fault locations, insulators flashed, surge voltages measured, lightning-stroke-recorder records, surge-crest-ammeter measurements, structure-footing resistances, and so forth.

The original 64.7-mile line without overhead ground wires operated very poorly for 1 1/2 seasons with 27 lightning tripouts.[18] In addition lightning caused a Wallenpaupack transformer to fail within 3 1/2 months, and a second transformer failed three weeks later. The line was then operated at 66 kv and all transformers at Wallenpaupack and Siegfried were rebuilt at the factory. All were found to be well along the road to failure, but since rebuilding and the installation of protective spillway gaps at their terminals, no more failures have occurred. During 1927, 24 line miles were equipped with conventional 184,000-circular-mil ACSR overhead ground wires spaced ten feet six inches above conductors at towers. Figures 1 and 2 show the general construction design. Lightning flashovers still occurred on the "protected" section, and the 1928 record for the entire line was 15 lightning tripouts, at least one of which was on the overhead ground-wire section. By this time other interconnected 220-kv lines, completely equipped with similar overhead ground wires were used, and during that year these circuits experienced 5 lightning tripouts.

It was evident and has since been thoroughly proved that overhead ground wires are very desirable but are not necessarily fully effective. The next move was to lower the resistance of tower-footing grounding. As an experiment a 2.6-mile long continuous 2/0 copper cable, buried where possible, and connecting 14 towers over High Knob Mountain,[5,18] was installed early during 1929. This region had still experienced flashovers, and during subsequent years lightning strokes of large magnitude have been measured to steel structures and overhead ground wires, and the effects of lightning strokes have been observed to nearby poles and trees. No insulator flashovers have occurred since this pioneer installation of continuous counterpoise was made, and it has proved to be an excellent investment, as well as serving as a model for other lines and other utilities.

By 1930 the single season's record was not accepted as proof that continuous counterpoise, averaging in length 1,040 feet per tower, was necessary, and so each structure in 16.8 miles of line having overhead ground wires was equipped with four 50-foot-long cables (crowfeet). During 1931 the remaining 4.6 miles of line having overhead ground wires were similarly equipped. Results to date have been excellent but not perfect. During 1936, 24 towers having the higher resistances had one cable per structure increased in length by 200 feet or to 400 feet of cable per tower, but these installations likewise have not had a perfect record of complete lightning protection. Figure 3 shows schematic plan views of tower-footing-grounding cable design.

During 1930 again as an experiment a 3.8-mile section of line was equipped with nonconventional elevated shielding wires,[18,19] insulated from the tower top and supported by 40-foot-long wood poles and four to six guy cables per structure, which were bonded to the tower base by long crowfoot-type cables. These "diverting cables" have had a perfect record for lightning protection, but experienced one mechanical failure, due to imperfection in a cable socket. Figure 4 shows the general design employed.

During 1938 and 1939 the 36.9 miles of line still unequipped with overhead ground wires were provided with buried galvanized three-eighths-inch steel continuous counterpoise cable, and the resulting low tower-footing resistances apparently avoided a few line flashovers. During 1940 and 1941 this section of line was also provided with two overhead ground wires of conventional configuration and elevation above line conductors.

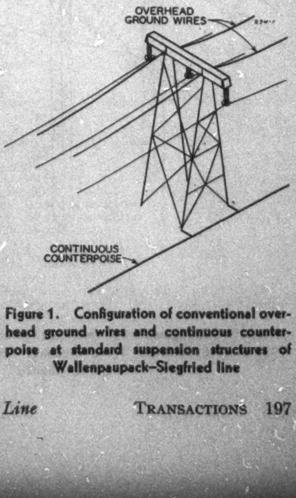

Figure 1. Configuration of conventional overhead ground wires and continuous counterpoise at standard suspension structures of Wallenpaupack–Siegfried line

Figures 2 and 5 show the design employed to support the overhead ground wires at standard suspension structures and at mast structures, respectively. Since the completion of the overhead ground wire no tripouts or flashovers have been experienced on the entire Wallenpaupack-Siegfried line. Inasmuch as this line is now completely equipped with overhead ground wires, with 39.5 miles of continuous counterpoise, and all structures in the remaining 25.2 miles having noncontinuous grounding cables which have a record of avoiding all but three or possibly five flashovers during 11 to 12 years, it is anticipated that the future lightning record of this line will be nearly ideal.

Of the references cited number 18 gives an excellent summary of the investigation up to 1938, including history, construction, photographs, measurements, results, and conclusions, and the reader is urged to refer to it.

Overhead Ground Wire and Buried Tower-Footing-Grounding Cable Construction

In Table III are listed data on lengths of line equipped and construction details. Of particular interest is the fact that the 1940–41 overhead ground wires were installed at 40 per cent of the estimated cost of duplicating 1927 construction. This saving was the result of a combination of circumstances. The selection of smaller and lighter conductor resulted in not only a saving in the cost of conductor material, but sufficient reduction in transverse loading on the suspension towers so as not to require any strengthening in order to maintain the desired factor of safety. The conductor selected was also of sufficient mechanical strength to maintain proper sag and clearance characteristics with a normal tension of 16 per cent of the ultimate which was felt to be well within the limits necessary to prevent any damage from high-frequency vibration.

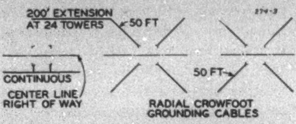

Figure 3. Plan of various tower-footing-grounding cable designs used on Wallenpaupack–Siegfried line. For diverting cables see Figure 4

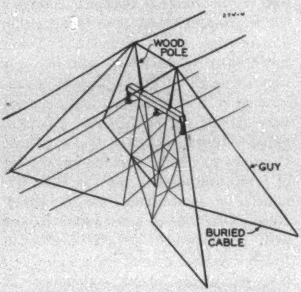

Figure 4. Configuration of 1930 lightning-stroke diverting cables, employing two wood poles to support and insulate the overhead ground wires from the structure

The mast type of structure used at angle locations as well as on very long spans necessitated a special design of additional structure due to the fact that operating conditions made it imperative that the line be restored to service every evening. The existing mast structures supporting the conductors could not be dismantled, lengthened, and rebuilt as was done during 1927. The design finally selected called for a pair of guyed steel masts, installed entirely independent of the existing triple mast structure, and supporting the overhead ground wires in their proper positions by means of a bridle arrangement. These 11 pairs of new steel masts were installed at approximately 40 per cent of the 1927 cost of dismantling and reconstructing existing mast structures. The bridle arrangement also provided excellent shielding over all the dead-end insulator assemblies at these points.

For counterpoise the use of galvanized steel has the advantage of reducing material costs over copper or other acceptable material, minimizing danger of theft (which generally exists where copper or copper-coated steel conductor is used), and providing grounding facilities at least very nearly as good as and possibly better than with smaller-size conductor ma-

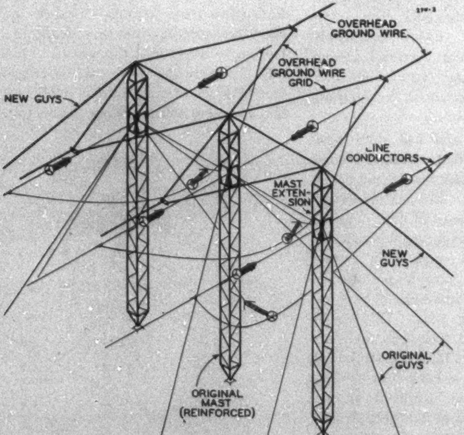

Figure 2. Configuration of overhead ground wires installed during 1927 at mast structures. These structures were rebuilt, strengthened, and lengthened

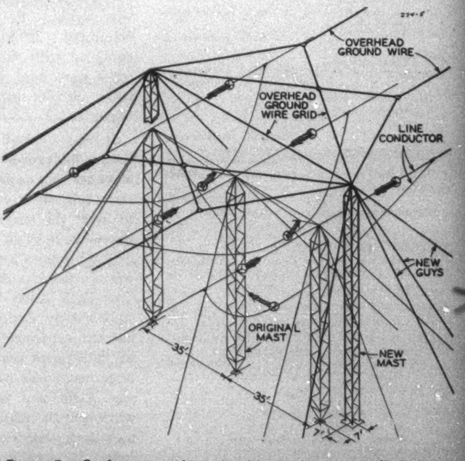

Figure 5. Configuration of overhead ground wires installed during 1940 and 1941 at mast structures. The original structures and guys were not altered. The additional masts and guys are entirely independent and self-supporting structures

terial. It is believed that this steel cable will function adequately for many years.

Tripouts of Wallenpaupack–Siegfried Line

During 1932, 47.1 additional circuit miles were added to the original 64.7 miles. The new circuit extended from Bushkill (roughly midway between Wallenpaupack and Siegfried) to Roseland, N. J. During 10 years, 9 tripouts occurred on the new circuit, and during 16 years, 295 tripouts occurred on the original circuit. Of these latter, 8 occurred on overhead ground-wire sections as follows:

1—Caused by sleet on line conductors (1933).

1—Caused by diverting guy-cable failure (1938).

3—Caused by direct lightning strokes before tower-footing grounding had been improved (1928, 1929, 1930).

2—Caused by direct lightning strokes after tower-footing grounding had been improved (1930, 1932).

1—Same as above, except that it also involved mechanical failure of an insulator assembly (1939).

Of the remaining 287 faults, 20 were caused by incorrect relay operation, operating mistakes, foreign objects against line conductors, system instability, or other causes, and one involved mechanical failure of an insulator assembly caused by a direct lightning stroke. Most, if not all of the rest, totaling 256 tripouts were caused by direct lightning strokes to unprotected line conductors. No permanent damage ever resulted except burns on insulators and occasionally shattered porcelain which was replaced later on a maintenance schedule.

In 1928, 1930, and 1932, other 220-kv circuits, totalling 283.7 miles equipped with twin conventional ACSR overhead ground wires, were placed in 220-kv interconnection service. On these, 98 tripouts have occurred at a rate of 2.7 tripouts per 100 circuit miles per year. The majority of these faults was caused by lightning. For the Wallenpaupack-Siegfried line where overhead ground wires were in use, 3 faults in 3 years were occasioned by lightning prior to installation of improved grounding and 3 faults in 11 years were caused by lightning subsequent to installation of crowfoot grounds. These figures reduce to 4.6 and 1.3 tripouts per 100 circuit miles per year and substantiate the conclusion that the Wallenpaupack-Siegfried line is subject to lightning and that, although once very vulnerable, is now well shielded.

Performance of Buried Tower-Footing-Grounding Cable

In reference 7, Tables II through V, and Figures 1 and 2, data are presented on lightning-current measurements in lightning rods, overhead ground wires, tower-structure members, and buried cables. Measurements have not been made directly of current flowing through tower footings, but measurements of currents in structure members and buried grounding cables show:

(*a*). Continuous counterpoise conducts up to 75 per cent and on the average 58 per cent of the total structure current; noncontinuous crowfeet conduct 11 to 48 per cent of the total structure current depending upon their lengths, and combinations of four crowfeet per structure conduct 42 to 70 per cent of the total structure current.

(*b*). The presence of continuous counterpoise causes 77 to 83 per cent of the total structure current to crowd to that side of the tower. Since the counterpoise conducts 58 per cent of this current, the footings must conduct the remainder or 19 to 25 per cent as compared with 17 to 23 per cent for the other two footings. The presence of one 250-foot crowfoot and three 50-foot crowfeet causes 52 to 58 per cent of the total structure current to crowd to the tower corner having the long crowfoot and leaves 14 to 16 per cent of the tower current in each of the other corners. Since the crowfeet conduct 70 per cent of total structure current, the remaining 30 per cent is distributed among the four footings, and there seems to be no tendency for any one footing to conduct more current than any others.

From these measurements it might be concluded that noncontinuous crowfoot cables are all that are required to produce ideal grounding, and that they are superior to continuous counterpoise. A number of other factors should be mentioned and may help explain why continuous counterpoise so far has a perfect performance record, whereas noncontinuous crowfeet cables have not; why we feel that economically continuous counterpoise is best suited to our conditions; and why we prefer this method of grounding on our 220-kv and 66-kv lines.

1. The 13-year record from High Knob is for continuous cable installed over a stony mountain where lightning has long been known to be prevalent. The cable is not buried at all locations. The inherent tower-footing resistances range from 50 to 148 ohms. The three largest lightning-rod current measurements were 57,000, 62,000, and 65,000 amperes. For this 14-tower section during 1926 (with no overhead ground wire or counterpoise in use) at least 6 insulator assemblies were flashed. During 1927 and 1928, 23 more flashed insulator assemblies were found. Overhead ground wires were in use for about three-fourths of this time and at least two of the flashovers occurred during 1928, while overhead ground wires were in use, but prior to installation of counterpoise. For the seven-year period, 1935 through 1941, at least 25 large lightning strokes have been measured; 21 ex-

Table III. Data on Overhead Ground Wires and Buried Tower-Footing-Grounding Cable Installations

Overhead Ground Wires			
Year installed	1927	1930	1940–41
Length in miles	24.0	3.8	36.9
Conductor material	ACSR	ACSR	Copperweld
Conductor size	184,000 cir mil	184,000 cir mil	$^7/_{16}$ inch special E.H.S. 7 strand
Spacing between cables	22 feet-7 inches	38 feet	22 feet–7 inches
Elevation above conductors at suspension towers	10 feet-6 inches	50 feet	10 feet-6 inches
Percentage suspension towers requiring strengthening	100	100	0
Percentage mast towers requiring rebuilding	100		0
No. suspension towers equipped	115	18	160
No. mast towers equipped	4	0	11
Percentage of towers where Stockbridge dampers were applied to line conductors and overhead ground wires	100	0	0
Tower-Footing-Grounding Cable			
Year installed	1929	1930–31	1938–39
Length in miles of line equipped	2.6	25.2	36.9
Number of structures equipped	14	123	171
Conductor material	Copper	Copper	Galv. steel
Conductor size	2/0 Stranded	2/0 Stranded	$^3/_8$" Stranded
Nomenclature	Counterpoise	Crowfeet	Counterpoise
Configuration	Continuous along one side of line and bonded to 2 tower footings	4 50*-foot-cables perpendicular to each other	Continuous along one side of line and bonded to 2 tower footings
Depth buried	About 1 foot	About 1 foot	About 1 foot in wooded areas, and $1^1/_2$ to 2 feet elsewhere
Extensions added**	None	To 24 towers	To 5 towers

*At 18 structures equipped with 1930 diverting cables, crowfoot cables were much longer in order to connect with the guy-cable anchors. Two structures had 6 guys and 6 cables.

**Extensions added to crowfeet consisted of 200 feet of cable added to one of the 4 existing cables. Extensions added to counterpoise consisted of two 250-foot-long crowfeet per structure.

ceeded 20,000 amperes, 8 exceeded 40,000 amperes, 2 exceeded 60,000 amperes, and the highest was 70,000 amperes.

2. The record of noncontinuous crowfoot-type cable shows three flashovers during two years before cables were installed and five flashovers during the 10- to 11-year period since cables were installed. Three of these latter flashovers caused line tripouts, but two apparently did not. In no case are lightning-current measurements available. In the last case (1939), an insulator-assembly failure also occurred at a tower having originally 114 ohms resistance. The failure was due to the spreading of a cracked insulator cap at the time of an unusually severe lightning stroke, but it is not certain that this insulator failure and line tripout would have occurred, had the insulator cap been in perfect condition, and it may be that the long crowfoot cables would, otherwise, still have a perfect performance record.

3. At the time the High Knob counterpoise and crowfoot cables were installed, 2/0 copper conductor was used, and it was all installed by hand labor (pick and shovel). In recent years it has been felt that galvanized steel counterpoise is perfectly satisfactory, and the 1938–39 installation consists of 36.9 miles of three-eighths-inch Siemens Martin stranded guy wire. It was, in general, installed by means of a tractor and special "counterpoise plow," which digs a trench 18 to 24 inches deep, lays the cable, and partly backfills the trench. The labor and material cost of installing galvanized steel radial crowfeet exceeds the labor and material cost of installing such a counterpoise.

4. Since the Pennsylvania Power and Light Company is not confronted with exceptionally dry and sandy soil conditions where deeply driven rods may be superior to buried cable, and since, except in very rare cases, no additional grounding is required beyond that provided by a single continuous counterpoise, we feel that, for our conditions, such installations are both electrically and economically ideal. For towers having exceptionally high footing resistance, supplementary grounding can be added. This was done for five towers over a mountain where the underlying strata are of slate formation and where resistances were 200, 237, 319, 165, and 329 ohms.

Another factor which may help explain the performance of continuous counterpoise is that current measurements are obtained at many towers, even seven or eight towers distant from the stroke, or $1^{1}/_{2}$ miles away in both directions. These measurements show a definite division of current at each structure, with part of the incoming current diverted up the structure and the remainder flowing in the counterpoise toward the stroke. At the end of the span, the counterpoise current is increased by current picked up from the earth. This current increase is of the order of 10,000 amperes in the span nearest the stricken tower, and drops to 1,000 amperes three or four spans away. In a similar manner, the overhead ground wire currents increase at each tower by the amount going up the structure, and thus serve to conduct currents as high as 30,000 or more amperes in stricken overhead ground-wire spans, and currents on the order of 10,000 amperes for strokes to structures.

On the other hand, where crowfoot cables are used, measurable currents in crowfeet are usually limited to 3 or 4 structures instead of 12 to 14 structures. Continuous counterpoise thus enables the lightning stroke to be fed by many multiple grounds (for example 10,000 to 14,000 feet of buried cable and 50 or more tower footings) rather than by 1,000 to 2,000 feet of cable and 12 to 16 tower footings. Current measurements obtained since 1933 when plotted on charts similar to Figures 1 and 2 of reference 7 are very striking. They confirm our belief that continuous counterpoise is a very effective device for conducting lightning currents and keeping tower-footing potentials low, and that continuous counterpoise is adequate for our conditions.

Summarized Results

1. Lightning-surge voltages were early found to be caused by negative strokes in almost every case. Practically every positive-voltage record was found to have occurred at time of insulator flashover at the tower and line conductor to which the surge-voltage recorder was connected. Such positive voltage records were caused by the recorder and earth being temporarily positive with respect to the stricken line conductor. Many thousands of surge-crest-ammeter records have likewise rarely indicated positive lightning strokes and never any of large magnitude.

2. Measurements of induced voltages, or usually failure to obtain measurements of induced voltages and absence of flashovers which were not clearly associated with direct lightning strokes, showed 12 to 15 years ago that induced lightning voltages are insufficient to cause trouble on lines insulated for 220-kv operation.

3. Hundreds of measurements of voltage across 40 to 50 feet of structure steel seldom indicated voltages exceeding 40 kv, and practically never indicated voltages exceeding 50 kv, and showed that most of the rise in tower potential at times of lightning strokes occurred in the earth surrounding the footings.

4. Twenty-eight measurements of voltage in the earth around tower footings at times of lightning-current discharge indicated that 20 per cent of the potential drop from tower to true earth, exists within five feet of the footings. The voltage across five feet of earth exceeded 50 kv and 40 kv for seven and five cases, respectively. In eight other cases the discharge seemed to be of long continued duration rather than a rapid and violent impulse.

5. Well-defined lightning-current measurements show that strokes either contact structures directly or else one (but not both) of the overhead ground wires between structures. For unprotected sections of line, it was early found that the middle conductor rarely flashed over, but that the outer conductors (and particularly the conductor nearest to approaching storms) experienced frequent flashovers. Furthermore, nearly all flashovers were confined to one conductor with two flashed assemblies (one at each of two adjacent structures) being the usual condition.

6. No flashovers are known to have occurred to overhead ground-wire structures of the Wallenpaupack-Siegfried line having meggered tower-footing resistances of less than 13 ohms. For towers of about 20 ohms resistance, strokes up to 50,000 amperes usually if not always are safely conducted, and strokes up to 100,000 amperes have been safely conducted occasionally.

7. Increasing the number of insulator units on outside conductors from 14 to 16 during 1929 and 1930 resulted in no discernible improvement. After the two transformer failures during 1926 and examination of the remaining transformers, all eight single-phase units were rebuilt, and spillway protective gaps set at $42^{1}/_{2}$ inches were installed on each phase conductor near their terminals. No transformer failures have since occurred, and periodic examination has disclosed no more lightning damage, but spillway gap flashovers were frequent at Wallenpaupack until 1934 when 220-kv lightning arresters were in successful operation. Since then three spillway gap flashovers have occurred, but none were on the two protected phases. Of 15 arrester current discharges measured, 6 exceeded 1,500 amperes.

8. During 1930 fault location was facilitated by cathode-ray oscillograph measurements of the time between surge reflections from the faults and from line terminals. Since that time fault-location estimates have been made by relating dynamic-current measurements obtained at Wallenpaupack, Siegfried and Roseland substations.

9. Many cathode-ray oscillograph time-voltage records were obtained during 1928, 1929, and 1930 of lightning voltages on line conductors. The first oscillogram of this nature was obtained during 1928 at the Wallenpaupack laboratory. Another was occasioned during 1930 by a typical negative stroke close to the oscillograph, and, contacting the conductor to which it was coupled, and, causing line insulation flashover less than 100 feet away showed a negative rise in voltage exceeding 2,760 kv and with a rate of rise of 1,540 kv per microsecond between the ranges of 750 and 2,760 kv. This record is believed to be the only one of its kind ever obtained.

10. Unfortunately, the High Knob lightning laboratory equipped with a cathode-ray oscillograph and special cameras has failed to obtain any important data. This has been due to failure of strokes to contact the lightning rod on the structure over the laboratory building, or the overhead ground wires within one span, during periods of laboratory operation. The lightning rod

on the laboratory tower was struck during 1936, 1937, 1938, and 1939 by negative strokes measuring 33,000, 62,000, 38,000, and 17,400 amperes, and strokes measuring 15,000 and 47,000 amperes occurred to the overhead ground wires in the immediately adjacent spans during 1936 and 1938. Of the 25 large strokes measured to the counterpoise section of line between 1935 and 1941 13 were to lightning rods on the five towers atop High Knob summit.

11. None of the measuring devices or auxiliary equipment used has ever caused a service interruption, and none has ever failed with the exception of a few lightning-stroke recorders connected to measure voltage near tower footings.

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Lightning Investigation at High Altitudes in Colorado

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I. Introduction

THE operation of transmission and distribution lines in the high altitudes of the Rocky Mountain region had indicated that lightning strokes were not so severe as at lower altitudes. Also glow discharges or corona currents from the earth have been observed at high altitudes from pointed objects and rocks. This investigation was made to determine the probable lightning current at altitudes from 6,000 feet to 13,500 feet and to measure corona current. It has been found that the probable stroke current decreases with increase of altitude from sea level to approximately 18,000 feet altitude at which point it appears that no current would be present. The highest mean temperatures at 18,000 feet do not exceed 32 degrees Fahrenheit, and comparison of temperatures in free air at altitudes up to 13,500 feet check temperatures on the earth's surface and indicate that freezing temperatures may limit the formation of lightning.

Measurements of corona current from the earth during lightning storms indicated as much as 480 microamperes with a potential gradient of 94 kv per foot.

II. Description of Line and Territory

The Shoshone–Denver 100-kv transmission line of the Public Service Company of Colorado was constructed in 1908 and 1909 on steel towers and is not equipped with overhead ground wires. However, a single continuous counterpoise wire has been installed from Leadville to Denver and a double counterpoise over the high passes at Argentine and Hagerman. Figure 1 shows a profile of the line and the location of the counterpoise wire and magnetic links. These counterpoise wires are buried in the ground to a depth of approximately one foot wherever possible. The line passes through the heart of the Rocky Mountains starting at Shoshone Plant at an altitude of 6,000 feet and terminating at Denver at an altitude of 5,280 feet, passing over the Continental Divide three times: Hagerman Pass, altitude of 12,000 feet; Fremont Pass, altitude of 11,300 feet; and Argentine Pass, at an altitude of 13,500 feet. The average altitude of the line is about 10,000 feet.

The type of territory varies widely from plains and hills to rugged mountains and includes cultivated lands, sand and gravel, and various rock formations, some of which are mineralized.

III. Measurements and Measuring Equipment

It had long been noted that lightning strokes at high altitudes did not appear to be so destructive as at lower altitudes, as indicated by the operation of transmission and distribution lines and it was assumed that the currents in lightning strokes might be smaller. Measurements were started in 1937 by installing surge-crest ammeter links on some of the towers. The complete investigation was underway in 1938 with brackets on certain towers where past experience had indicated the most lightning activity, and brackets were placed on the counterpoise on most of its length. The surge-crest ammeter links have been described elsewhere.[1] In some cases a complete installation of brackets was made on a tower which included four legs and eight braces. Figure 2 shows one of the typical towers with brackets on one of the legs and two braces.

In 1940 a corona and surge-voltage-recording installation was made on top of Argentine Pass at 13,500 feet elevation, and a similar installation a few miles west of Denver at about one mile elevation.

The installation at Argentine Pass is shown in Figure 3, and the connection arrangement in Figure 4. To measure corona currents at the tip of the lightning rod, a recording microammeter was connected between the lightning rod and the ground. For protection of the microammeter should a direct stroke hit the

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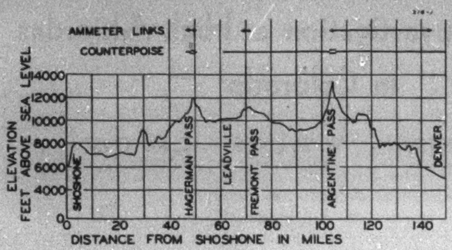


Figure 1. Profile of Shoshone–Denver 100-kv transmission line, showing ground elevation above sea level, and location of counterpoise and surge-crest ammeter links

lightning rod, an inductance was connected in series and a protective gap in parallel. Lightning-rod discharge currents are a measure of cloud field gradient at the earth's surface. Accordingly, current measurements were especially desired immediately before and after direct lightning strokes. To record the time of such strokes and to measure crest-current values a series resistance was inserted in the lightning-rod circuit and a surge-voltage recorder connected across this resistance. Magnetic links were mounted on the lightning-rod down lead to obtain a check reading of crest-current values. The recording microammeter was adjusted to indicate both positive and negative currents. The surge-voltage recorder, because of its double polarity registration characteristics, was capable of measuring crest-current values of either positive or negative lightning strokes.

With one such installation at the high elevation of Argentine Pass, one at the medium elevation of Denver, and a third at a much lower elevation on a line in Eastern Pennsylvania, it was planned to obtain data on discharge levels and polarities over a wide range of elevations. Accompanying this program of field-data taking is a laboratory investigation in which the discharge characteristics of points under different conditions of electric field configuration are being studied.

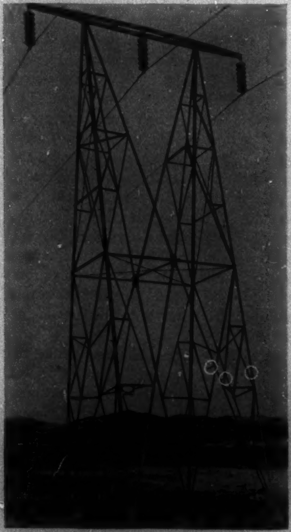


Figure 2. Typical tower showing location of surge-crest ammeter brackets (circled) on tower leg and braces

IV. Direct-Stroke Record

After each lightning storm the transmission-line patrolmen checked the magnetic links, and those which were magnetized were sent into the central office where they were calibrated and each case studied to determine the tower and stroke current. This information was co-ordinated with the patrolman's observations of the time of day of the lightning storms and location along the line. This was checked with the records and oscillograms of Petersen coil operations and line tripouts. In this way it was possible to locate where one-phase-to-ground, two-phase-to-ground, and three-phase faults occurred and to determine tower and stroke current for each case. After the stroke current was calculated the altitude of the tower was determined, and a point was plotted on Figure 5 which shows stroke-current values against altitude. Table I shows a summary of the stroke currents for various altitude groups, namely, 6,000 to 8,000 feet, 8,000 to 10,000 feet, 10,000 to 12,000 feet, 12,000 to 14,000 feet, for the four years 1938 to 1941 inclusive. Each one of these groups was analyzed individually to determine the probable stroke current in the group.

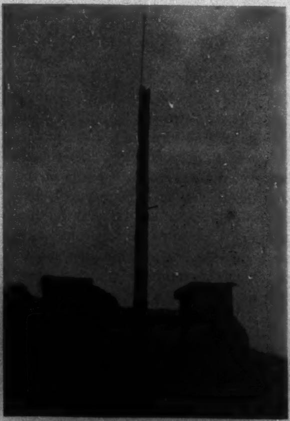


Figure 3. Installation on Argentine Pass at altitude of 13,500 feet for measuring earth corona current and surge voltages

The probable stroke current curves are shown in Figure 6. The upper curve has been presented by Lewis and Foust[2] and was derived from investigations made at altitudes below 2,100 feet. It has been used to represent conditions at low altitudes. The other curves indicate the probable lightning current as derived from the Colorado investigation for three altitude groups as well as a summarizing curve for this investigation, extending from 6,000 to 14,000 feet. The curves indicate a decrease in probable stroke current with increase in altitude. This conforms to observations over 30 years of operation in this district.

The curves in Figure 6 were studied to determine the relation of this decrease of current with the increase of altitude. This was done by selecting the values of current on the five per cent abscissa line for the various altitude group curves of Figure 6. Thus were obtained the four points in Figure 7 marked with circles, including one point at sea level, one at 7,000 feet, one at 11,000 feet, and one at 13,000 feet, and curve *A* was drawn through these points. The average altitude for each group was used as the ordi-

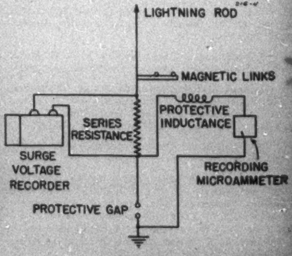


Figure 4. Schematic diagram of earth-corona-current and surge-voltage-recorder equipment

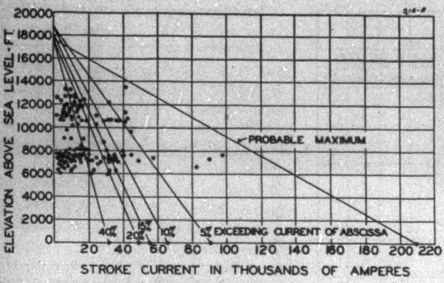


Figure 5. Elevation above sea level at which the strokes occurred on the Shoshone–Denver 100-kv line and the probable maximum current at various altitudes from sea level to 20,000 feet

This also shows the current for various percentages of maximum current at the same altitudes

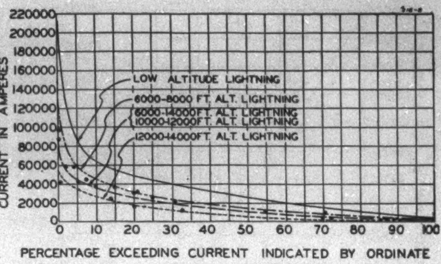


Figure 6. Cumulative curves of probable stroke currents from 734 strokes on transmission lines at low altitudes,[2] and 145 strokes to the Shoshone–Denver 100-kv transmission line at altitudes from 6,000 to 13,500 feet

nate. In obtaining the point in Figure 7 marked with a square, the value of current which the highest five per cent of all observed strokes exceeded was plotted against the average of the altitudes at which occurred the strokes in this highest five per cent. Line *B* was then drawn from the five per cent value for sea level, obtained from the upper curve of Figure 6, through this last point marked with a square. This line intersects the zero current at about 18,000 feet.

It will be noted in Table I that the greatest number of strokes observed was in the group from 6,000 to 8,000 feet altitude. Also, it can be noted in Figure 7 that the five per cent point for this altitude group is the closest to line *B*. This shift of the points in the various altitude groups has been noted from year to year and it is assumed that with a larger number of observations the two lines would approach an intermediate line indicated as line *C* of Figure 7. The same procedure was used for the highest 10 per cent, 15 per cent, 20 per cent, and 40 per cent of the stroke currents, and the resulting curves are plotted on Figure 5. All of these lines of Figures 5 and 7 appear to intersect the zero current at about 18,000 feet altitude. This had been noted in 1938 and has continued through each year of the investigation. It therefore appeared to be of interest to attempt to verify this indicated absence of lightning above 18,000 feet and to attempt to find an explanation.

V. Temperature Observations

Temperature is one of the factors which decreases with increase of altitude, and studies were made to determine the relationship which this might bear to the conclusions previously mentioned. A graphic thermometer was set on top of Argentine Pass during the summer of 1941 to obtain temperatures at 13,500 feet, since the highest altitude for which Weather Bureau information is available is at Leadville which is at an altitude of 10,200 feet. Data were also obtained from the Denver weather bureau of temperatures in the upper air as obtained by balloon sondes and described elsewhere.[3] Data for the year October 1940–September 1941 are presented in Table II. The straight lines in Figure 8 indicate, for example, the variation of temperature with altitude from the balloon readings for three days in July 1941. The corresponding mean temperatures for Argentine Pass, Leadville, and Denver are also indicated. It will be noted that the temperature in free air corresponds rather closely with temperatures at the three locations just mentioned. In Figure 9 are similar average curves for each month of 1941 and for the year, plotted from the data of Table II. This figure indicates that the mean temperatures on the earth at any particular altitude correspond very closely to those at the same altitude in free air, and it appears that the temperature which might exist at altitudes on the earth at 18,000 to 20,000 feet would be as indicated in Figure 9. This shows that at this altitude the temperature never exceeds 32 degrees Fahrenheit, which may be a significant fact.

Minser obtained observations from airplanes,[5] and Figure 10 is a generalized

Table I. Range of Probable Stroke Currents at Various Altitudes

1938, 1939, 1940, 1941

Altitude	6,000 to 8,000 Feet					8,000 to 10,000 Feet					10,000 to 12,000 Feet					12,000 to 14,000 Feet					
Amperes	1938	1939	1940	1941	Four Years	1938	1939	1940	1941	Four Years	1938	1939	1940	1941	Four Years	1938	1939	1940	1941	Four Years	All Years
1,000– 5,000	6	3	14	6	29	2	0	0	0	2	2	2	3	0	7	4	0	0	0	4	42
5,001– 10,000	1	0	6	2	9	2	0	1	0	3	0	1	3	2	6	4	0	1	0	5	23
10,001– 20,000	10	5	3	3	21	4	0	2	2	8	0	1	7	2	10	2	0	1	0	3	42
20,001– 30,000	0	0	5	1	6	1	0	1	0	2	0	1	3	0	4	0	1	0	0	1	13
30,001– 40,000	5	1	5	0	11	1	0	0	0	1	0	0	2	0	2	0	0	0	0	0	14
40,001– 50,000	0	1	1	0	2	1	0	0	0	1	0	0	2	1	3	0	0	1	0	1	7
50,001– 60,000	0	1	0	0	1																1
60,001– 70,000	0	0	0	0	0																0
70,001– 80,000	0	0	0	0	0																0
80,001– 90,000	1	1	0	0	2																2
90,001–100,000	0	0	0	1	1																
Total	23	12	34	13	82	11	0	4	2	17	2	5	20	5	32	10	1	3	0	14	145
Total strokes					82					17					32					14	145

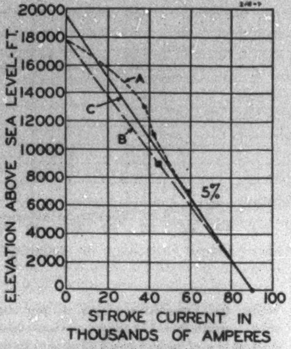


Figure 7. Illustrates method of obtaining percentage lines in Figure 5

Shows the value of the five per cent largest strokes occurring in altitude bands of 6,000 to 8,000 feet, 10,000 to 12,000 feet, and 12,000 to 14,000 feet, plotted against mean altitude for each band. Also shows the average altitude of the five per cent largest strokes

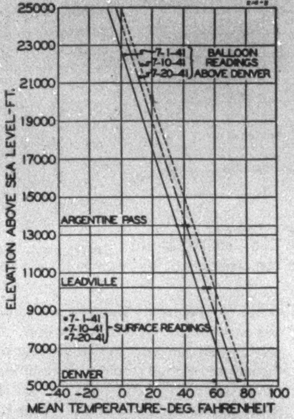


Figure 8. Temperature in free air above Denver from balloon sondes up to 25,000 feet altitude for July 1, 10, 20, 1941, and mean temperatures at Denver, Leadville, and Argentine Pass on corresponding days

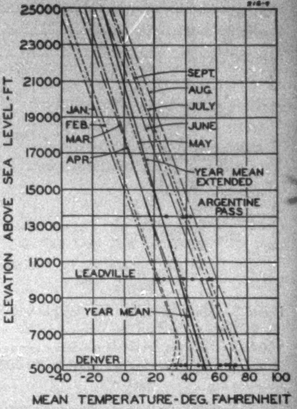


Figure 9. Balloon sonde mean temperatures above Denver for January through September, and for the year 1941, for altitudes up to 25,000 feet, and mean temperatures for same months at Denver, Leadville, and Argentine Pass

diagram showing that at altitudes between 12,000 and 20,000 feet there is a mixture of sleet and water with temperatures between 32 degrees Fahrenheit and −6 degrees Fahrenheit. This region of somewhat mixed charge lies between the upper region predominantly positive and the lower region predominantly negative. From an analysis of a large number of actual cases, he found that lightning discharges to aircraft in flight occur most frequently in the zone adjacent to the freezing isotherm, that is, from 26 degrees Fahrenheit to 34 degrees Fahrenheit. The mean altitude at which discharges to aircraft were encountered was 10,000 feet with a maximum of 18,000 feet and a minimum of 2,000 feet, with no one level predominant.

Simpson and Scrace[6] also found by sending up balloons that a separation of charges in the cloud takes place, the lower portion of the cloud being predominantly negative and the upper portion predominantly positive, and that the region of separation between the negative charge and the upper positive charge occurs at a level where the temperature is between −10 degrees centigrade and −20 degrees centigrade (14 degrees Fahrenheit and −4 degrees Fahrenheit), as shown in Figure 11. The recording thermometer on Argentine Pass near the microammeter showed temperatures ranging from 26 degrees Fahrenheit to 51 degrees Fahrenheit at times when records of corona discharge were obtained.

Transmission line patrolmen have observed for a long time that lightning might occur in a snow storm, but it had also been noted that under these conditions the temperature was near freezing and accompanied by wet heavy snow. This has taken place at all altitudes.

All of these observations indicate some correlation between temperature and altitude and the presence or absence of lightning discharges, but the exact relation is still somewhat obscure. However, the observations do indicate that freezing temperatures may limit the formation of lightning.

Another set of contributing factors pertinent to the decrease of lightning current with altitude are the following: The breakdown strength of air decreases about three per cent per 1000 feet, so that at the altitudes of this investigation the breakdown strength of air varies between approximately 60 and 80 per cent of the sea level value. With less voltage available between cloud and earth, it would be expected that the lightning-current values would be lower. Furthermore, it is possible that in some cases the line itself is very close to the cloud, if not in the cloud. In such cases the difference of potential between cloud and line would be very small. In such high altitudes many discharges would be expected, each of small current magnitude, and further, some altitude would be reached at which disruptive discharges would be of insignificant proportions. This is for the reason that the projection of the mountains into the cloud regions limits the accumulation of charges by increasing cloud-to-ground leakage, at the same time precipitating discharges before cloud charge can build up to average low-altitude lightning proportions.

Table II. Balloon Sonde Temperatures in Air Above Denver and Mean Temperatures at Denver, Leadville, and Argentine

		Mean Temperatures			Balloon Sondes at Denver (Ft)			
		Denver	Leadville	Argentine	5,000	10,000	15,000	20,000
January	1941	34.3	21.0		33*	20*	0*	−21*
February	1941	37.2	25.6		31*	23*	2*	−18*
March	1941	37.2	20.4		41*	31*	12*	− 6*
April	1941	46.8	31.4		50**	32**	12**	− 8**
May	1941	61.0	44.8		55**	38**	19**	0**
June	1941	65.6	49.4	40.0	70**	50**	28**	7**
July	1941	72.8	55.6	39.0	75**	55**	34**	13**
August	1941	71.7	55.2	38.0	80**	60**	38**	17**
September	1941	61.7	46.0	26.0	68**	54**	33**	11**
October	1940	55.8	41.2		46*	42*	25*	7*
November	1940	37.8	25.4		36*	28*	14*	0*
December	1940	35.6	25.0		0*	0*	−15*	−36*
Year		51.2	36.7		49	36	16.8	− 2.8

*Observations at midnight only.

**Average of observations at noon and midnight.

VI. Effective Counterpoise Resistance

Tower-footing resistance tests were made in 1934, using ground Megger, before counterpoise wires had been installed. This was a very dry year with only 8.93 inches precipitation which was 63.5 per cent of normal. Tests were made on various towers ranging in altitude from 6,000 to 13,500 feet and located in various types of earth including clay or loam, sand and gravel, and rock. The resistances are plotted in Figure 12 against altitude and found to vary from 40 ohms to 950 ohms. The same towers were tested again in 1935 when the precipitation was 17.2 inches or 21.8 per cent in excess of normal. The results are also plotted in Figure 12 and show that the resistance varies from 30 ohms to 360 ohms. These curves indicate that the moisture had a considerable influence on resistance values. The curve also shows that the resistance increases with increase in altitude. This may be accounted for by washing of salts from the ground by rains and snow and by the high-resistance type, of rocky territory encountered at high altitudes.

During the course of observation of lightning strokes and their measurements, the record of the one-phase-to-ground, two-phase-to-ground, and three-phase faults was noted. This was obtained from automatic oscillograph, Petersen coil operation, and relay indications. The line operation was co-ordinated with the stroke measurements to determine what type of fault occurred. For the four years covered by the investigation, 145 strokes were recorded, of which 56 produced single-phase-to-ground faults, 20 two-phase-to-ground and 5 three-phase faults, 64 strokes were observed where no line faults were produced. It is probable that a majority of the strokes occurred to a conductor, thus causing flashover at an adjacent tower, sometimes with very little current in the stroke. If the stroke current were of sufficient magnitude so that the product of current times footing resistance exceeded the flashover of the insulator strings, then one or two additional phases might flashover, possibly influenced by the phase position of the generated voltage. Strokes to towers would flash over one, two, or three phases, if the tower were raised in potential above the insulator string flashover, the number of phases depending on the condition of the individual strings and the phase position of the generated voltage.

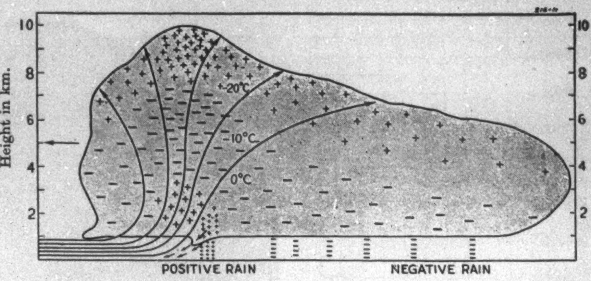


Figure 11. Generalized diagram by Simpson and Scrace,[6] showing air currents and distribution of electricity in a typical heat thunderstorm

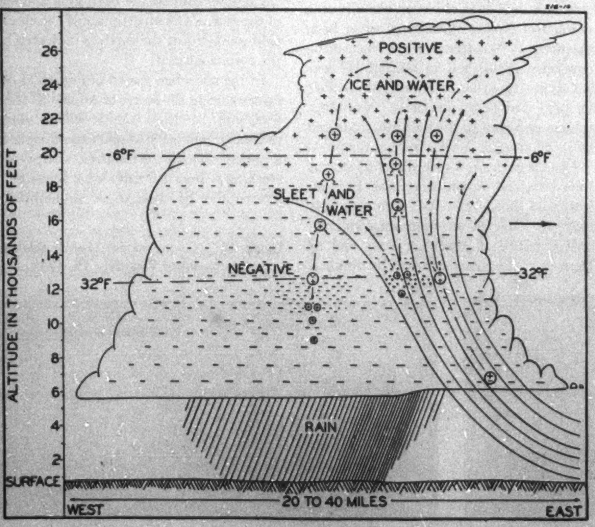


Figure 10. Generalized diagram by Minser,[5] showing the distribution of meteorological elements and mechanism of the separation of electrical charges in a typical thunderstorm

Table III shows the length of counterpoise at which readable values of current were read. These distances are plotted against current magnitude in Figure 13. This figure indicates that the greater the lightning current the greater the distance readable current was carried by the counterpoise. A possible explanation for this is that the larger current values are associated with more extensive clouds. The electrostatic field under these clouds covers a large area, and on the occurrence

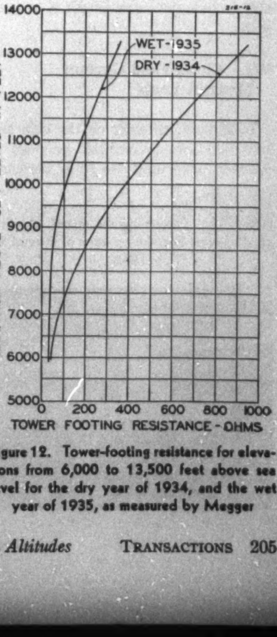


Figure 12. Tower-footing resistance for elevations from 6,000 to 13,500 feet above sea level for the dry year of 1934, and the wet year of 1935, as measured by Megger

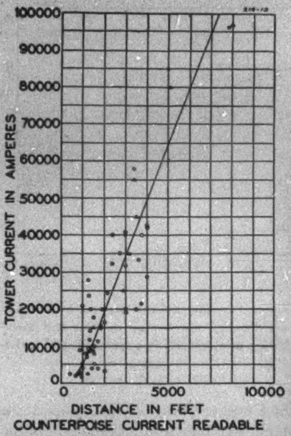


Figure 13. Length of counterpoise at which readable values of current were read for various tower currents

Triangle points are from Table III

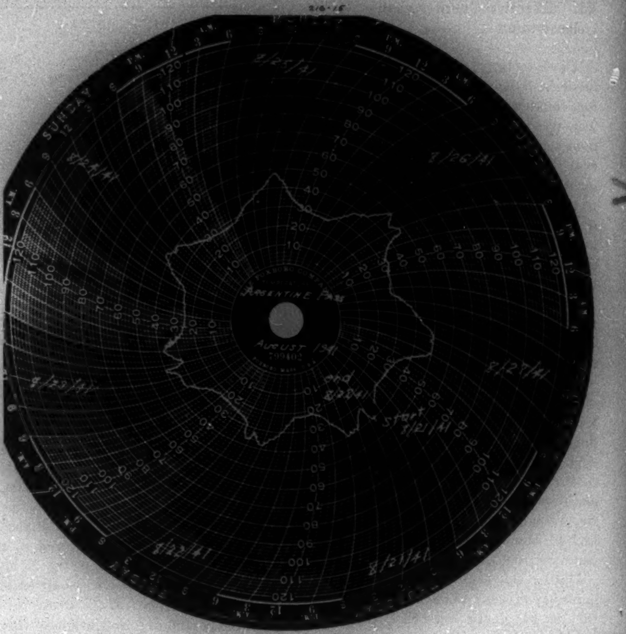


Figure 15. Thermometer record at Argentine Pass during the period corresponding to the corona record of Figure 14

of a stroke, thereby collapsing the field, current flows from the entire area into the stroke. Much of this current makes its way to the extensive counterpoise system, which offers a low resistance path to the tower involved in the stroke. However, since only a portion of the entire counterpoise is involved with any one cloud, the effective resistance of the counterpoise is always somewhat greater than indicated by Megger measurements, which presumably include the entire length of counterpoise.

VII. Cloud-Field Current Measurements

Investigational work on the high altitude Shoshone–Denver line provided an excellent opportunity to study discharge currents from the ground surface during lightning storm periods. Such corona streamers had been observed visually on a number of occasions, but had never been measured. Accordingly, the measuring equipment described in section III was set up at Argentine Pass and near Denver in 1940 and again in 1941. No direct strokes occurred to the lightning rods and no surge-voltage records were obtained.

Discharge-current records for 51 storms were obtained on the Argentine Pass installation during the 1941 lightning season. In Table IV are summarized all records obtained at Argentine Pass and at Denver. The latter are few in number, particularly because the recorder was not in operation during a portion of the period of data taking. This table shows time of occurrence of a storm, the maximum amplitudes of both positive and negative polarity currents occurring in each storm, the temperature at the beginning and end of the storm, and an estimate of the cloud-field gradients at the earth as indicated by the corona current.

In the specimen record of Figure 14 obtained during the storm of August 21 from 9:00 p.m. to 10:30 p.m. it will be noted that beginning at 9:10 and extending over a period of some 20 minutes, sharply increasing corona currents were repeatedly interrupted by discharges, undoubtedly

Figure 14. Corona-current record obtained on Argentine Pass during storm of August 21, 1941

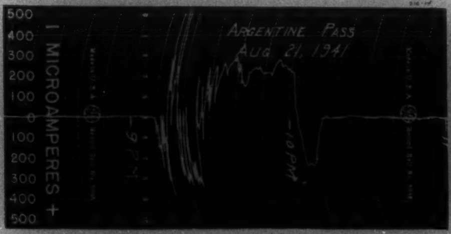
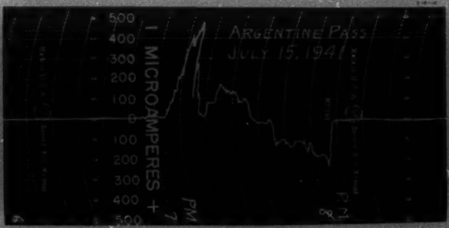


Figure 16. Corona-current record obtained on Argentine Pass, July 15, 1941



due to nearby lightning strokes. The extreme deflections of the microammeter pointer which leave only single line traces on the record show that a lightning stroke occurred with resulting abrupt change in field gradient. The amplitudes of these wide swings are due partly to sudden release of induced charge in the lightning rod, that is, induced surge voltage and overswing of the sensitive instrument movement. Following this early period of the storm came a steady flow of rod discharge current extending over a period of about 40 minutes and of negative polarity. During the next five or six minutes there occurred a gradual change of cloud field from negative to positive polarity without the sharp lightning stroke changes and apparently due to the movement of a positively charged portion of the cloud to the vicinity of the lightning rod. Several records of this general type were obtained during the 1941 lightning season. In Figure 15 is reproduced the thermometer record at Argentine Pass during the period corresponding to Figure 14.

Typical of a storm not accompanied by frequent nearby lightning discharges is the record shown in Figure 16 and obtained at Argentine Pass on July 15 from 6:50 p.m. to 8:20 p.m. This record shows a gradual increase in gradient beginning at 7:05 p.m. over a period of 10 minutes and reaching a high negative gradient of 94 kv per foot at 7:15 p.m. At this time, a single stroke discharged this high gradient down to about half of its previous maximum level. Continuing, the gradient changed gradually to positive in about 20 minutes, building up to a gradient of 75 kv per foot, which disappeared within three or four minutes time, presumably as this portion of the cloud moved away from the lightning rod.

In considering the records obtained at Argentine Pass, it is well to carry in mind that this elevation places the lightning rod well up into the cloud field on many occasions. Under these conditions the ideal case of a lightning rod projecting from the earth surface up into a uniform field is not obtained. Such an arrangement could, therefore, not be used in the laboratory to calibrate for gradients indicated by the Argentine Pass setup. The tip of the lightning rod at Argentine Pass extends 18 feet above the level of the earth surface and it is presumed that cloud charges may at times be within a very few feet of the rod. Accordingly, in the laboratory calibration, corona currents were measured for a long rod extending more than 10 feet above the grounded plane with the tip located only four feet away from a high potential plane which was some three feet in diameter. Voltage from a continuous voltage-rectifying-type generator providing up to 500 kv was applied to the high potential plane over a wide range of voltage levels of both positive and negative polarity. Corresponding rod currents were measured for each voltage level applied. The figure for voltage gradient was obtained by dividing applied voltage by the distance between rod tip and high potential plane. Correction was made for the relative air density of approximately

Table III. Length of Counterpoise at Which Readable Values of Current Were Read

Tower Current	1938 (Feet)	1939 (Feet)	1940 (Feet)	1941 (Feet)	Average (Feet)
0– 5,000	1,220		650	1,450	1,110
5,001– 10,000	1,300	1,300	900	1,500	1,250
10,001– 20,000	1,730	2,150		1,500	1,460
20,001– 30,000		3,850	2,000		2,920
30,001– 40,000	2,870		3,250		3,060
40,001– 50,000	3,000			4,000	3,500
50,001– 60,000		3,300			3,300
60,001– 70,000					
70,001– 80,000					
80,001– 90,000		5,000			5,000
90,001–100,000				8,000	8,000

Table IV. Cloud-Field Current Records

1	2	3	4	5	6	7	8	9	10	11
Location	Month and Day 1941	Time		Current in Microamperes		Cloud-Field Gradient Kv Per Foot		Temperature Degrees Fahrenheit		
		Start	End	Pos.	Neg.	Pos.	Neg.	Start	Min.	End
Argentine Pass	June 21	11:25A	1:10P	130	20	64	30	50	39	51
	22	1:00P	6:50P	20	30	30	36	52	38	39
	24	2:00P	6:00P	230	115	75	59	48	29	29
	July 5	2:20P	2:50P	260	63	78	47	56	51	51
	5	3:45P	4:50P	478	240	94	76	41	36	36
	5	6:40P	7:30P	68	0	50	0	32	31	31
	9	3:35P	4:10P	100	20	51	30	56	50	50
	10	4:30P	5:00P	40	0	40	0	52	47	47
	11	1:10P	2:20P	350	200	85	72	56	40	40
	11	4:00P	5:10P	340	230	84	75	40	34	34
	11	5:30P	7:00P	100	70	57	50	33	30	30
	12	5:10P	5:50P	110	210	59	72	44	44	45
	14	2:10P	2:40P	120	0	60	0	47	45	46
	14	7:20P	8:10P	20	20	30	30	35	33	33
	15	3:30P	4:10P	100	0	57	0	40	40	41
	15	6:50P	8:20P	240	480	75	94	36	29	29
	17	7:20P	7:40P	170	63	68	49	42	39	39
	18	6:40P	7:20P	78	43	52	42	39	37	37
	18	7:40P	8:30P	20	103	30	58	36	33	33
	19	3:20P	5:00P	170	153	68	67	43	42	42
	19	5:10P	5:50P	0	172	0	68	39	35	35
	19	8:40P	9:10P	150	22	66	31	34	34	34
	21	8:30P	9:00P	48	0	44	0	33	33	34
	21	9:30P	11:10P	0	22	0	31	34	34	34
	Aug. 1	5:00P	6:10P	100	0	57	0	49	48	48
	3	8:20P	9:00P	42	38	41	40	34	34	34
	5	4:20P	5:30P	58	32	47	36	41	41	41
	5	7:20P	10:20P	120	190	60	70	38	36	36
	7	2:00P	3:00P	195	32	70	36	44	39	39
	10	1:20P	3:00P	240	200	76	71	44	34	34
	10	5:10P	6:40P	28	200	35	71	38	34	34
	12	2:40P	3:20P	58	125	47	62	46	41	41
	12	3:50P	4:50P	78	72	52	50	39	36	36
	14	1:20P	3:00P	68	103	50	58	44	32	32
	15	7:40P	8:50P	40	42	41	41	34	33	33
	18	2:30P	3:10P	240	202	76	71	31	31	33
	18	3:30P	4:30P	0	95	0	56	33	33	38
	18	9:20P	10:00P	210	45	72	43	30	30	30
	19	10:30P	11:00P	0	118	0	60	28	28	28
	20	6:50A	7:10A	0	50	0	44	26	26	26
	20	8:50A	9:20A	58	0	47	36	30	28	28
	20	10:20A	11:00A	50	72	44	50	30	30	32
	20	6:00P	7:00P	0	133	0	62	37	32	32
	21	11:40A	12:10P	20	160	30	66	38	34	34
	21	9:00P	10:30P	300	300	91	72	30	25	25
	22	1:20P	2:20P	140	20	64	30	36	33	33
	22	4:40P	5:20P	140	40	64	41	34	28	28
	23	2:10P	3:00P	290	100	80	57	34	30	30
	29	6:10P	6:50P	0	40	0	41	36	35	35
	Sept. 1	11:50A	12:40P	160	108	67	58	42	36	36
	3	8:40P	9:30P	230	62	74	48	29	26	26
Denver	Aug. 12	3:30P	6:00P	122	142	61	65			
	21	12:30P	2:00P	24	42	33	41			
	23	1:30P	4:00P	60	102	47	58			
	Sept. 1	11:30A	2:00P	30	142	36	65			

0.6 at Argentine Pass. The positive and negative current values were about equal for the same voltage levels. The use of such a laboratory setup, of course, involves speculation as to field conditions and means that the gradients indicated are at this time only approximate. However, additional field and laboratory data and its correlation will provide more positive results as the investigation proceeds.

In columns 5 and 6 of Table IV, maximum current values for each storm are tabulated. Both positive and negative values are recorded for about 80 per cent of all storms, the remaining 20 per cent giving either positive or negative current exclusively. Discharge currents ranged up to 480 microamperes both positive and negative. A positive instrument current means that the rod tip is positive relative to the ground and indicates a positive-inducing cloud field. A negative current means that the rod tip is negative relative to the ground and indicates a negative-inducing cloud field. Planimeter surveys of all records show that integrated time and amplitude for positive polarity clouds was 55 per cent and negative polarity clouds 45 per cent.

Columns 7 and 8 of Table IV give equivalent voltage gradients as indicated by the laboratory calibrations previously described. These gradients range up to 94 kv per foot. In 1930 two of the authors reported field-gradient measurements at the ground surface up to 87 kv per foot at about 1,200 feet above sea level.[7] These early results were obtained on an instrument of the ballistic galvanometer type which was connected to the cloud field through a capacitance arrangement and which recorded only on the occurrence of a lightning stroke. The average record was below 10 kv per foot. The new measurements range substantially above the earlier values as would be expected due to the close proximity of the cloud to the lightning rod.

VIII. Conclusions

1. One hundred and forty-five stroke currents were measured at high altitudes, ranging in value from 2,000 to 96,800 amperes, with 64 per cent negative and 36 per cent positive. These figures should be compared with figures for low-altitude transmission lines, in which approximately 95 per cent of the strokes were of negative polarity and 5 per cent positive. It is possible that on the high altitude line, where the line may at times be within the cloud, the positive portion of the cloud may be discharged to the line more frequently than in the normal case.

2. Strokes caused 56 one-phase-to-ground faults, 20 two-phase-to-ground faults, and 5 three-phase faults. The remaining 64 strokes caused no line disturbances.

3. The observed stroke current decreases with increase of altitude from sea level to 13,500 feet.

4. Results indicate that there may be no lightning strokes if the ground level is above an altitude of 18,000 feet.

5. The mean temperature at an altitude of 18,000 feet in free air above Denver never exceeds 32 degrees Fahrenheit.

6. Mean temperatures which would prevail *if* the earth's surface were at 18,000 feet elevation may be the same as that obtained from the Balloon Sondes and may never exceed 32 degrees Fahrenheit.

7. The mean temperature for Leadville (altitude 10,200 feet) and Argentine Pass (13,500 feet) conforms very closely to temperatures at corresponding altitudes in free air as obtained by Balloon Sondes at Denver.

8. This investigation and other observations indicate that freezing temperatures may limit the formation of lightning.

9. The length of counterpoise at which readable values of current were read increases directly with increase of stroke current.

10. Current in tower legs and braces divides approximately as the cross-section area of the steel, although erratic readings were obtained in many cases.

11. Counterpoise wires carried practically all the current with little in tower footing.

12. The microammeter measuring earth discharge at Argentine Pass showed indications for 51 different storms up to 480 microamperes. Potential gradient was calculated from these records up to 94 kv per foot.

13. Polarity characteristics of field gradients at 13,500 feet as determined by the integrated area under the amplitude–time curves, were 45 per cent negative and 55 per cent positive. Comparison of maximum discharge currents per storm also showed positive and negative polarities to be about equal. Particular storms, however, are occasionally predominantly positive or negative.

14. The Shoshone–Denver line passes over an area of differing geological formations but, except for resistivity, these formations do not appear to influence the locations where lightning strokes occur nor the value of current in a stroke. In fact, contour of the ground and its relation to the direction of travel of the storm appears to have more bearing on the location of the strokes than geological formations.

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Supplementary Control of Prime-Mover Speed Governors

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THE development of interconnected systems has required improvements in the manual and automatic control of load, frequency, and time. The speed governors of the prime movers have always provided the medium for supplementary control of frequency, load, and time. Experience with various types of control devices and the factors influencing their functioning have been discussed frequently in the technical literature. Recently the problem has been given renewed attention[1-5] with an effort to correlate experience with theory and to provide a basis for determining the proper characteristics of governors[2] and their supplementary control.

In attacking this problem, the authors and their associates first made an analysis of the prime-mover governors when performing their essential function of speed control.[1,2] Such an analysis was necessary to provide the required background for the problem of system control of frequency and load by means of supplementary devices. The work on prime-mover governors resulted in conclusions as to their desirable characteristics. This paper presents what appear, from analysis and experience, to be logical conclusions as to the proper characteristics and functions of the supplementary control devices, and, accordingly, is a sequel to the previous paper dealing with prime-mover governors.[2] At the same time this is a companion paper to the one by Concordia, Weygandt, and Shott,[6] presenting the results of an analysis of tie line control. The problems of load, frequency, and time control have usually been treated as related to the operating problems of particular systems. This paper has as its purpose a discussion of the various factors which apply more generally for all types of systems.

System Operation

Economic generation of power, to always meet the demand, to satisfy transmission facilities, and, at the same time, keep frequency and time within certain limits, gives rise to a variety of problems. Every power system or section of a power system is subjected under normal conditions to load changes of rather large magnitude during the course of its daily operation. Most systems have appreciable load "drop offs" and "pickups" during the morning, noon, and evening hours. These load changes may be of the order of one to two per cent of system capacity per minute. Around the average load may be a fringe due to loads which are more rapidly applied and removed. Also, occasionally emergencies arise which will cause sudden changes in loading and frequency.

The characteristics of all the control devices of a system, whether automatic or manual, determine its performance during these changes and readjustments. The co-ordination of the functioning of these devices becomes increasingly important with the number of interconnections and the size of the individual loads which may be applied, and the intelligent solution will necessarily be a compromise among all the requirements involved. After reviewing the factors which appear to be essential for the proper control of generator output, tie-line load, frequency, and time, we have reached the following conclusions:

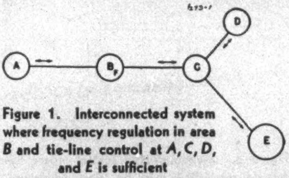

Figure 1. Interconnected system where frequency regulation in area *B* and tie-line control at *A*, *C*, *D*, and *E* is sufficient

1. The duty on the regulating stations can be reduced, for the same allowable frequency deviation, by using more governors which have the proper characteristics.

2. Automatic frequency control may be obtained with simultaneous operation at several stations provided with time-error droop correction.

3. Maintaining absolutely flat frequency, time and tie-line loads is an unnecessarily severe requirement.

4. The required rates of response on the larger systems, with a reasonable amount of regulating capacity, are well within the ability of the prime-mover equipments and controllers.

5. Large rapid load changes are difficult to confine to an area because of limitations in energy supply and of inherent time lags.

The following discussions of the essential factors support the above conclusions.

System Speed Governors

All of the active speed governors of a system tend to support the frequency following system changes in load. Their contribution is made at a rate comparable with the rate at which the loads are usually applied. Parallel-operated machines require some regulation in order to operate properly in parallel. A previous investigation[1] has indicated that the optimum regulation in order to keep the frequency deviation to a minimum, without resulting in undue or unnecessarily long periods for the readjustment to take place, was of the order of about six per cent incremental regulation for steam units. Also, in practice hydroelectric units with droop-correction mechanisms usually end up with an adjustment of around five per cent on the larger systems.

Tests on systems indicate that the composite or effective system regulation is considerably greater than the optimum and may be more of the order of 10 to 20 per cent because of dead band, operation of some machines with blocked governors, and operation at wide open valve points. An approximate characteristic for some systems has been found to be a one per cent load change for a 0.1 of a cycle frequency deviation. This corresponds to an over-all system regulation of 17 per cent. This difference between over-all system regulation and the regulation of an individual unit tends to encourage blocking the governors of the new and efficient units with modern governors so that they will not be able to take more than their share of load changes. This aggravates the situation by resulting in a still larger composite regulation. Experience has indicated that the lower the composite regulation of a system (not so low as to result in too small damping) the smaller will be the frequency change resulting from a suddenly applied or rejected load of a given magnitude. Thus, the more correct solution[8,11] would be to increase the number of active governors and decrease the dead band of all these governors to as small values as is practical.

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It should be recognized that, until a reasonable percentage of capacity is equipped with governors having small dead band and more uniform incremental regulation, units with improved governors will be doing more than their proportionate share of governing. If a steam unit has a six per cent incremental regulation and no dead band, the load change on that unit for a ±0.1 of a cycle fringe in system frequency will be ± 3 per cent of rating. If the instantaneous frequency deviation is reduced to ±0.05 of a cycle by an over-all improvement in governing alone, the change in load on such a unit will be reduced to ±1.5 per cent of rating, without any increase in regulating station rates of response. Thus, the support of system frequency would be distributed among units having more nearly the optimum regulation, and comprising a greater percentage of the system capacity. This would result in more rapid damping of load oscillations and in more stable operation, with less frequency deviation. Furthermore, the load changes on individual plants which were not assigned the duty of regulating would be so small that they could be considered essentially base-load stations. This improvement in system performance is the reward for reduction of dead band and for the employment of governors having more uniform incremental regulation. It is important, on the other hand, to realize that the regulation of individual units should not be made less than the optimum, for then the frequency fringe may actually be increased rather than decreased. Since there exists an *optimum regulation* it does mean that (even though it is not particularly critical) if the regulation of system is too small, an unnecessarily long time for the damping out of frequency oscillations may be required, while if it is too great, the frequency deviation may not be as small as could be realized otherwise.

Exceptions arise in system operation when it may not be desirable to have all units operating with active governors. It may be desirable to block the governor for small frequency changes (for example, a magnetic pull-out device[9]) of a steam unit at light load in order to prevent variations in output which may be large in comparison with the load the unit is carrying, although small in comparison with its rating. Also, for a base-load unit with high efficiency or with a boiler and boiler control unable to cope with the normal load changes, it may be considered necessary or advisable to operate the unit blocked. These exceptions, however, are less likely to exist when a larger number of units are operating with active governors and the frequency deviations are carefully kept within close limits by supplementary control.

FREQUENCY CONTROL

Allocating the system generation has ordinarily been accomplished among generating stations by communicated dispatches. The control of frequency and the responsibility for holding frequency can, of course, be distributed in this same manner. However, to hold frequency within close limits requires a constant readjustment at the regulating stations. It was a very natural step that this duty should be largely taken over by automatic control and should be assigned to particular stations whose incremental costs, depending upon the system loading, were such as to make them the most economical units for this control. Many systems are operated under a semiautomatic and manual control of frequency. The frequency regulating station is changed during the day or the loading of other units of the system is changed in order to allow the regulating units to operate within their capacity to hold frequency. The use of automatic control at the station or stations which are assigned to the duty of holding frequency at a particular time relieves the operating personnel of a very onerous task. In addition, since automatic control is constantly at work restoring frequency to normal, closer frequency regulation is obtained with the result that there is less tendency for manual stations to contribute by their own initiative. This usually results in better system operation and less unnecessary transfer of load between stations. In general, the loads which are most critical to frequency deviation on a system do not require as close a frequency regulation as is desired, by the system operators, for the best operation of the system.

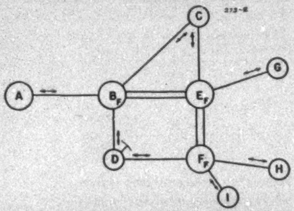

Figure 2. Interconnected system where frequency regulation may be used advantageously in areas *B*, *E*, and *F* with tie-line control at *A*, *C*, *D*, *G*, *H*, and *I*

It has become general practice to use one station for holding frequency and time. The other regulated units control tie-line loads. In systems of this type, one station of the whole interconnected group, no matter how large, or a group of closely tied stations, controls frequency. This has come about naturally, it being a fairly easy way to distribute the responsibility among regulated stations and still use the simplest type of control. This type of control is particularly well-adapted to a system interconnected[3,7] as shown in Figure 1, where the other sections of the system are separated from the section holding frequency by only a few tie lines. These other sections can be assigned the duty of holding tie-line loads.

However, for the type of system shown in Figure 2, it becomes more difficult to assign the individual stations or areas to regulate tie-line loads and it may become simpler for two or more stations to be assigned the responsibility of regulating frequency. This is not readily accomplished by the ordinary method used in frequency regulated stations of having a controller which holds essentially flat frequency without any direct control of time error. Parallel operated frequency controllers of this type have been operated when it was possible to have slow rates of response and to keep the two stations in range together by frequent communicated dispatches and manual readjustments. Completely automatic parallel operation can be accomplished, however, by providing the frequency regulating stations with mechanisms which measure accurately the time error and proportion their contribution to the frequency regulation of the system in proportion to their assigned regulating capacity and the time-error deviation. This control principle has been successfully used for many years and provides means for automatically dividing responsibility by what has been termed time-error droop correction,[10] employing a time-error range which is well within practical requirements for electrical time on the systems, that is, one second for complete regulating range.

ACCURACY OF STANDARDS, TIME, AND FREQUENCY LIMITS

Reasonable electrical time accuracy is one of the by-products of automatic frequency control. In fact, it is not difficult to provide acceptable electrical time on manual control with a master clock and Arlington time as reference.

In their enthusiasm, some operating groups, when operating as independent systems, endeavored to maintain very close to flat time. In principle, this means that following each interval of low frequency a forced high-frequency interval is required and vice versa, resulting in much unnecessary correction at the regulating station. This principle of operation is particularly troublesome in an interconnected area and imposes an unnecessary transfer across tie lines and an unnecessary burden on the regulating stations. Most systems have abandoned this ideal objective and are content with keeping time within ±1 to 2 seconds which is relatively easy to obtain, providing the regulating stations are always kept within range.

Flat frequency-control devices employing resonant circuits are not inherently good time standards. A continuous error of 0.01 of a cycle will integrate 14 seconds error in a 24-hour period. Thus, it is customary to supplement this type of control with some form of time standard, such as master clock, tuning fork, or crystal oscillator. The ultimate in time standard, of course, is a common continuous master frequency or time.[4] At present, the cost of the necessary equipments is the only obstacle to a widespread adoption of a common master standard.

Any of the available time standards is sufficiently accurate for backup purposes, as a supplement to straight frequency control so far as system time is concerned. Ordinary master clocks have an accuracy of one second per day; tuning forks one-half to one second per day; crystal oscillators about 0.1 second per day, and even Arlington time sends out its correction log sheets at the end of each month—revealing that its daily time signals may be off as much as 0.1 second. With any of these time standards, it will still be necessary for a system to have a correcting period, probably during early morning hours, to correct for drift in time standard or for system errors which may have accumulated when the controlling stations were not kept within range.

In appraising the value of a continuous master frequency, it should be kept in mind that it does not improve the parallel operation of flat frequency regulators. It merely applies equal amounts of calibration to all controllers in terms of the differential between system time and master time with the result that system time is maintained within prescribed limits and load equalization must still be accomplished by communicated dispatches and manual readjustments.

In contrast to this, the application of a master time standard to frequency controllers using the time-error droop principle results in perfect parallel control. Thus, the several frequency-control areas shown in Figure 2 may be used in any combination without frequent communication and without readjustment, except to satisfy changing system conditions.

The advantages of time-error droop control will be recognized as interconnections become more widespread and the number of frequency-regulating stations increase.

During the steady load periods the larger interconnected areas are able to maintain frequency within ±0.05 of a cycle and these limits may increase to approximately ±0.15 of a cycle during the rapid load changes. On smaller systems the steady fringe is more nearly ±0.1 of a cycle and the swings may reach ±0.25 of a cycle during pickup and drop-off intervals. The sudden load changes that are applied to the larger systems are necessarily smaller in proportion to the total capacity than may occur on the smaller systems—thus causing smaller frequency deviations for similar governor performance.

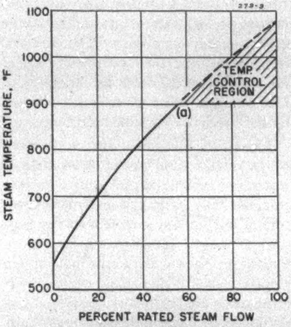

Figure 3. Typical temperature-flow characteristic of modern boiler with convection-type superheater

TIE-LINE CONTROLLERS

The control of frequency on an interconnected system is ordinarily a comparatively easy assignment and does not require a particularly fast rate of response. The tie-line controllers, on the other hand, are required to hold the tie lines within certain limits in order to prevent pulling out of step, overloading, or too great or long a load deviation from the assigned tie-line loadings, and it is logical that the tie-line controllers should have a proportionately faster correction rate than that of frequency-regulated stations. This also results in a more quickly balanced system after a load change occurs in that the generating capacity within the area in which the load change has taken place quickly picks up the load without requiring an unnecessary change in the frequency regulating station. As the regulated capacity responding to tie-line control increases, it is important to reduce the amount of unnecessary correction. This may be accomplished by employing the well-known principle of frequency bias.[3] Briefly, by combining frequency deviation and tie-line watt deviation, it is possible to determine the location of the net load change associated with these deviations, that is, whether it is internal or external to the area, and the tie-line controller thus responds for the portion of the load change within that area, and when properly adjusted allows the tie line to contribute to external load changes, by an amount equal to the composite governor regulation of that area.

In a closely knit system operating with a frequency control alone, the control may be as rapid in response as is permissible for frequency stability provided there is sufficient energy supply. In the limit this type of system could approach a single unit with a governor having zero speed droop and having as small a droop-correction time lag as may be obtained with stability.

The kilowatt transfer between two systems is not a steady quantity. Load changes in the several areas are continually being applied, and system constants, together with existing governor dead bands, do not produce critical damping to the original oscillation. Before the tie line has steadied down following a load change, another load change has appeared with the result that almost a continuous oscillation exists. This oscillation in watts may be as much as ten per cent of the tie-line capacity and occurs at a period varying between one and five seconds, depending upon the system inertias on either end and the synchronizing coefficient of the tie line. Thus, the watt sensitive element in a tie-line controller need not attempt to follow the instantaneous watts but rather the average of the fringe watts associated with this relatively fast oscillation.

TYPES OF TIE-LINE CONTROLLERS

A controller which is in equilibrium only when the controlled quantity has been returned to its desired value is called "the floating type." A controller which is in equilibrium when the controlled quantity has been returned to a value which deviates from the desired value by an amount proportional to the controlled quantity is called "the proportional type." In terms of regulation "the floating type" is a flat controller having zero regulation, while "the proportional controller" has a definite positive regulation.

Practically all supplementary controllers of turbine governors are of the floating type. The synchronizing motor receives impulses, either continuous or intermittent, until the controlled quantity has returned to the desired value. The proportional controller may be a device which exerts a pull on the governor arm in proportion to the deviation of the controlled quantity.

The analysis presented in the companion paper[6] shows that equally good performance may be obtained from the two types of controllers if they are properly designed. The proportional controller requires a sufficiently long time lag between the deviation sensitive element and the resulting proportional force that it exerts on the governor. The floating type requires a sufficiently slow rate of correction to allow for stable operation. Since the latter type is the most easily obtained, it is most generally used.

For quick response a continuous controller, either floating or proportional, is better than an intermittent type. A controller whose rate of correction or correcting force is proportional to the deviation is capable of better performance than one which does not have this proportional feature. In all cases, stability may be obtained by providing the controller with a sufficiently slow rate of correction or long time lag. Rates of response considerably greater than those generally used now may be obtained by proper design and do not necessarily require the use of a stabilizer. For best performance of a steam unit, with optimum controller design, the governor incremental regulation should be fairly uniform at a value of about six per cent, with small dead band.

TELEMETERING

In many cases the tie line to be controlled is not adjacent to the regulating station, and the watt indication is then telemetered. Except in a few special cases, relatively slow types of telemetering now are being used successfully for indication and control purposes. For these special cases continuous and instantaneous types of telemetering have been successfully employed.[12,13]

RATE OF RESPONSE

Systems operating with a large number of active governors in good condition and with supplementary control will have but small frequency deviations for the usual load changes. The loads are absorbed by the active governors, and then the supplementary control may be used to return the average frequency and tie-line loads to normal. Under such conditions the supplementary control need not be particularly fast in response, corresponding only to the rate at which the average system load changes. This then allows for a relatively slow change in the output of the frequency-regulating station. It has been found that within limits the greater the allowable frequency departure, the less is the required speed of response of the frequency-regulating stations. Also, the greater the number of active governors, the less is the required speed of the response of the frequency-regulating station in order to prevent the frequency departure exceeding that which is tolerable. Operation in this manner relieves the frequency-regulating units by taking advantage of the diversity of load changes and allows them more opportunity to correct the frequency at a rate consistent with their steam and hydraulic energy supply conditions. The governor mechanisms and their control ordinarily are capable of allowing for increases in load at a rate which exceeds that allowed by the steam or hydraulic conditions.

The steam units of a station, because of the smaller time lags of steam than of water, are capable of providing a quicker response than hydraulic or hydrogenerators, if not limited in prime energy supply. However, steam plants may be so limited in their steam rate and steam storage capacity that many steam-generating units are limited at the steam-generation end. Hydraulic units may, however, have sufficient storage capacity but may require an appreciable time lag depending upon ability to change the hydraulic power to the turbine rapidly. When all these circumstances are considered it may well be that a low head hydraulic plant provides a more rapid means for regulating than a steam unit.

STEAM AND HYDRAULIC LIMITATIONS

The prime-mover energy storage and rate at which it may be changed are very often the most important limitations of the rate of response of a regulating station. In some instances slow boiler-control response may be an unnecessary restriction on the amount of load which can be quickly picked up. The control of the boiler should be such as to allow for as rapid change in steam flow as is possible without resulting in undue stressing of boiler or turbine. Most of the experience to date with regulating stations has been obtained using lower temperature boilers and turbines; some having chain grate stokers and others slightly faster underfeed stokers. Limitations in regulating rates are established under these conditions largely by fuel bed condition.

Very little experience is available on the newer high-pressure high-temperature boilers and turbines, using pulverized fuel (with mills having some storage capacity) or the somewhat faster response gas and oil-fired high-temperature boilers as regulating stations. The temperature-flow characteristics of a modern boiler using convection-type superheaters are such as to result in a decrease in temperature with decrease in steam flow; particularly is this true for flows below point *a* of Figure 3. Also, in a steam turbine the temperature distribution changes with flow as shown in Figure 4 for constant initial temperature. This is further aggravated by reduction in initial temperature at reduced flows. Accordingly, a reduction in flow may result in temperature difference of an appreciably greater magnitude in the turbine than in the boiler. The stresses in the turbine and also in the boiler depend upon the rate at which this change in flow and of temperature is made. A given amount of load change may be made instantly, while larger load changes should be made more slowly. This reasoning applies primarily to periodic changes, as the stresses due to varying temperature changes are more likely to result in fatigue stress. Even high temperature boilers and turbines usually can be expected to withstand without fatigue stress the changes in temperature resulting from instantaneous changes in flow of at least 15 per cent of rated flow, in the region in which boiler temperature is under reasonable control, and 100 per cent flow in about 30 to 60 minutes, depending upon the case under consideration.

The demands of speed governors and their prime-mover energy supplies due to normal frequency changes do not impose a severe burden on the supply. However, in units used for regulating purposes with supplementary control, it may be necessary to provide means such that any supply limitations may be avoided. It is evident from Figures 3 and 4 that conservative practice on a modern high-temperature installation of this type would be to confine the regulating range to flows in excess of 50 per cent and to a rate which does not overstress the critical locations. In this range the temperature changes in the turbine are appreciably reduced for variations in flow and the regulating range and rate may be correspondingly increased. In areas responsive to tie-line regulators, the burden may be distributed over a number of stations[8] and, likewise, frequency regulators operating in parallel can be used to advantage. Thus, there is every justification for believing that the number of regulating units will increase, thereby reducing the burden on any one unit, just as the active participation by all units on governors reduces the burden.

On some present-day systems the regulating capacity is so distributed as to limit the required maximum rate to 10 per cent of regulating-unit capacity per minute over a limited range. This may be considered a moderate regulating rate in comparison with load changes encountered on some smaller systems with rough loads. For a system which has a rate of load change of one per cent per minute this would require a regulating capacity of at least 10 per cent of system capacity.

It will be recognized that in case of emergency, a boiler and turbine should be capable of dropping or picking up full load almost instantly if operating temperatures had been previously established following the starting-up cycle. Such operation, although tolerable in an emergency, cannot be considered good practice if done periodically, unless special provision for such operation has been made in the design.

Large Rapid Load Changes

For those systems in which the suddenly applied load changes are an appreciable part of the total connected capacity, it is found that the frequency deviation will be correspondingly greater. If this exceeds what is considered a tolerable limit, it becomes necessary to use supplementary control having a greater rate of response. This then puts a greater burden on the regulating station, and, in order to avoid too great a frequency deviation, it may mean a sacrifice in economy, or a provision for larger energy-storage capacity, or an increase in the rate at which the prime energy is generated. It is evident that, except for the change in system loading with change in frequency, the net load change must be absorbed by generating capacity, at the corresponding rate of load change. For systems where the load change is a large part of the total load this may become a serious problem.

A large strip mill load may have a load increase of 30,000 kw in 5 to 20 seconds. If it were necessary to follow this load change with regulating capacity comparable with present-day practice on the larger systems, over a million kilovolt-amperes would be required. Fortunately, this type of system is the exception rather than the rule. In these systems, experience has indicated that the limitation is primarily in the available steam supply, and when operated as isolated systems, speed governors alone are relied on for speed control resulting in corresponding large frequency changes. Unless special provision is made for steam supply capacity of the prime movers, large fluctuations in steam pressure will result.

Such a system which finds itself subjected to large suddenly applied loads; for example, a strip mill load which is large compared with its generating capacity, may interconnect with another system in order to help absorb the load changes. If this is done, the frequency deviation will be reduced, and a portion of the load change will be transmitted across the interconnecting tie. In general, the reduction in frequency deviation and the load change transmitted across the tie will be in proportion to the capacity of the external system to the total capacity. By interconnecting with another system in this manner, the local system makes use of the additional prime-mover energy capacity of the other connected system, the load changes being distributed among an increased number of actively governed generators. If the frequency deviation produced by the rapidly varying load is within tolerable limits without the use of supplementary control, little regulating capacity is needed to handle such a load because it will be removed as quickly as it is applied. Accordingly, the supplementary control need correct only the average frequency deviation. This provides a very practical and reasonable method for handling such rapidly varying loads.

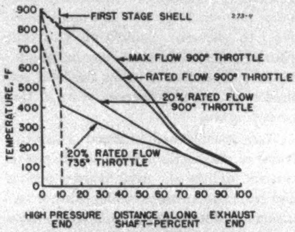

Figure 4. Temperature distribution in a modern high-temperature steam turbine

Occasionally there has been a demand for adequate control of rapid tie-line swings. In this case it becomes necessary to provide special control. The studies which have been made indicate that the limitation may not be in the control equipment so much as it is in the energy supply of the prime movers. From both analysis and experience, hunting or instability between the regulating units is more likely to occur when the rate of response of the regulating units is increased. For units which require a higher rate of response of their supplementary control, it is necessary for their governors to be in good mechanical condition with small dead band and to be stable mechanisms when operating alone. Under these conditions the rate of response with properly designed supplementary control can be further increased before the instability region is reached (see Figure 21—reference 6). The results of the analysis presented in the companion paper[6] indicate that with no limitation in energy supply the tie-line deviation may be held to 15 per cent for a continuous controller and 25 per cent for any intermittent controller for a load applied at a rate of 150 per cent of regulating capacity per minute, the regulated capacity being the total capacity of the local area and tied to the major system by a comparatively weak tie. Both this analysis and experience indicate that the control mechanism is not the limiting factor, since the energy supply limitation makes itself felt earlier.

Summary

It appears that the greatest improvement in over-all system frequency and tie-line regulation will be brought about by further improvement in the composite regulation of the system governing and by more widespread and co-operative application of supplementary control equipments which are already developed.

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PCC Car Operating Results in Pittsburgh

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Synopsis: The problem created by the decline in transit revenues during the depression led the Pittsburgh Railways Company to initiate a long-range service-betterment program. The most important phase of the plan was the use of modern street cars, as developed by the Presidents' Conference Committee, to replace the earlier types of rolling stock on the property. With 301 of these PCC cars in service, and 100 additional units scheduled for early delivery, sufficient data are available to show exceptionally favorable operating results. Actual experience in service has demonstrated that car maintenance comes down 28 per cent, track and roadway maintenance drops 21 per cent, accidents decrease 25 per cent, the number of cars required for specific schedules is reduced 10 per cent, and revenues increase 8 per cent. This favorable outcome has caused continued expansion in the use of such equipment and established a superior urban-transit service.

PEOPLE now have the utility in city transit which is so vital to the conduct of daily affairs. The streamlined PCC car, which gets its alphabetical name from the Presidents' Conference Committee sponsoring its development, provides a degree of excellence in public travel that has established a superior performance standard. Pittsburgh was one of the first cities to adopt this modern transit unit. A fleet of 301 cars is in operation and 100 additional vehicles are under construction. This is the largest installation of PCC cars, and, with a total of 401, there will be sufficient to operate base schedules on all street-car lines. Initial service dates back to August 1936, and the results are enlightening. Car maintenance has decreased 28 per cent, track and roadway maintenance has dropped 21 per cent, accidents per 10,000 car miles have come down 25 per cent, schedule car requirements have been lowered 10 per cent, and gross revenue has increased 8 per cent.

Operating Improvements Needed

During the lean years of the early '30's, the Pittsburgh Railways Company recognized the need for restorative measures to regain lost gross revenue and to lower operating costs. When the depression began to slow things down, one logical recourse was finding means of speeding things up. This involved re-examination of existing schedules to establish a program that would give service more beneficial to patrons. Coincident with this plan, it appeared essential to reduce maintenance expenditures below the level set up for the more prosperous years of the preceding era.

Many people had attained a critical frame of mind with respect to the street car, and rightly so. After years of seeing and riding in essentially the same noisy uncomfortable vehicle, such an opinion was understandable. Urban passengers responded to this neglect of their needs by transferring their patronage to the public carrier which provided adequate service. Modern street cars were needed to meet the demand for greater usefulness in city travel by providing service superior to any other vehicle for surface mass transportation. Action was required to give fast, convenient schedules, lower the cost of the car ride, relieve street congestion, and handle rush-hour traffic effectively.

Plans for Better Transit Service

Careful analysis of the transit problem led to the conclusion that immediate action was required for speeding up and reconditioning the outmoded rolling stock. Such a plan was put into effect to lower maintenance costs and arrest declining revenues. Motors were rewound for higher speeds, brakes were modified to obtain more effective stopping characteristics, and car interiors were improved.

It was recognized that the most satisfactory operation required modern street cars. However, the immediate benefits of existing rolling-stock improvements could be garnered pending the fulfillment of a long-range program for the procurement of new vehicles. Sufficient cars were reconditioned to handle more than 50 per cent of the mileage operated.

The plan for the purchase of new rolling stock was initiated with the installation of a single trial PCC car in August 1936. The first 100 cars ordered were placed in service from March through May 1937, the second 100 during November 1937, and the third 100 in April 1940. The fourth order for 100 cars will soon be ready for delivery. The type of car procured is shown in Figure 1, and the extent of the installation is illustrated by Figure 2.

Figure 1. Modern PCC car in operation in Pittsburgh

These modern cars have many outstanding advantages which are assisting in meeting transit problems in Pittsburgh. Some of the more important are as follows:

1. Performance is far in advance of any vehicle now using city streets. The speedy quiet-operating trolley car can save from 6 to 15 minutes of every hour's travel time in comparison with the older types.

2. A high degree of agility is attained with comfort to riders. Deep-cushioned forward-facing seats, wide clear-view windows, ample stanchioning, adequate ventilation, uniform distribution of heat, and plentiful indirect illumination conform to the demands of passengers.

3. The control and braking equipment permits attainment of a degree of smoothness of performance that is unexcelled in surface travel.

4. Schedule speeds are higher than any other form of public transportation on congested city streets. It is possible to use

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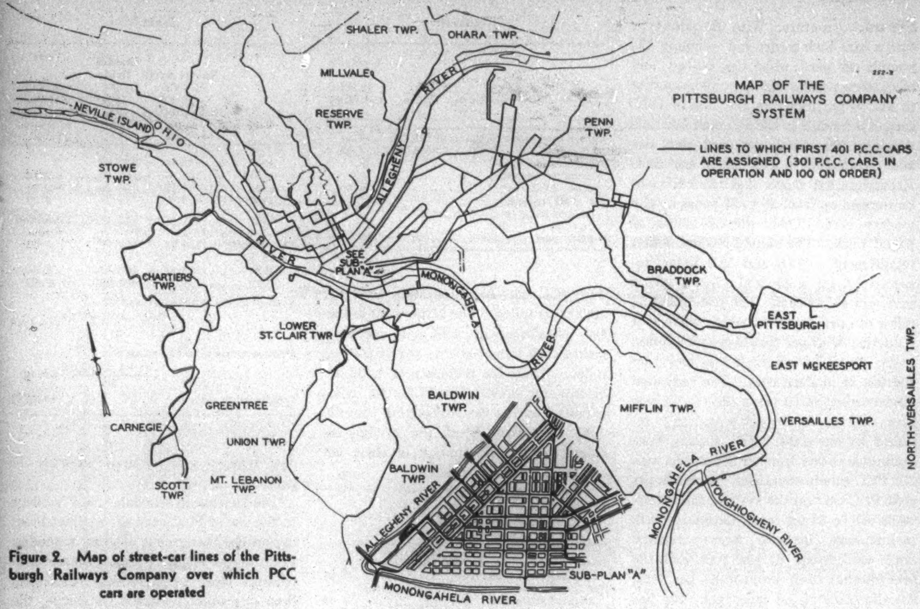
SHALER TWP.
OHARA TWP.
MILLVALE
RESERVE TWP.
ALLEGHENY RIVER
NEVILLE ISLAND
OHIO
STOWE TWP.
PENN TWP.
MAP OF THE PITTSBURGH RAILWAYS COMPANY SYSTEM
LINES TO WHICH FIRST 401 P.C.C. CARS ARE ASSIGNED (301 P.C.C. CARS IN OPERATION AND 100 ON ORDER)
RIVER
SEE SUB-PLAN "A"
CHARTIERS TWP.
MONONGAHELA
BRADDOCK TWP.
LOWER ST. CLAIR TWP.
RIVER
EAST PITTSBURGH
EAST McKEESPORT
NORTH-VERSAILLES TWP.
BALDWIN TWP.
GREENTREE
MIFFLIN TWP.
VERSAILLES TWP.
CARNEGIE
UNION TWP.
MONONGAHELA RIVER
BALDWIN TWP.
SCOTT TWP.
MT. LEBANON TWP.
YOUGHIOGHENY RIVER
ALLEGHENY RIVER
SUB-PLAN "A"
MONONGAHELA RIVER

Figure 2. Map of street-car lines of the Pittsburgh Railways Company over which PCC cars are operated

fewer units—10 to 20 per cent—a factor that is reflected directly in the fare cost.

5. Fast schedules and the corresponding reduction in the number of cars minimize traffic congestion and allows the most efficient use of city streets by the greatest number of people.

6. Starting is fully automatic with an unlimited selection of rates—speed of starting—to conform to the particular conditions of street traffic encountered.

7. Smooth electric braking is provided under automatic control. It functions at all times—even if the trolley loses its contact with the overhead wire. In addition magnetic track and air brakes are available.

PCC Car Operating Results

The rehabilitation of old cars to secure higher schedule speeds and reduce maintenance proved quite successful. Results were particularly beneficial in providing better service with lower operating costs. The investment required was more than justified and served to establish a transition to the important part of the program —replacements with modern cars.

PCC car operation has produced some attractive savings in vehicle inspection and maintenance. In comparison with the earlier-type cars, the wheel-grinding period has been doubled (20,000 to 40,000 miles), wheel life is increased from 140,000 to 200,000 miles, brake shoes last 15 times as long, and inspections have gone up from 250 to 5,000 or 10,000 miles, the higher figure being applicable to cars without hand brakes. Parts for PCC cars cost more, but fewer are subject to rapid wear, and the over-all result indicates less expense. Actual labor costs for specific jobs are about the same, so the decrease in replacements means less hours worked.

An analysis has been made of the comparative expenditures for the three-year period from June 1937 to June 1940. Materials are taken from storeroom requisitions and segregated between old and PCC cars. Labor is distributed in accordance with the number of cars passing through the shop—67 per cent earlier-type and 33 per cent PCC. Fixed expenses, such as superintendence, shop equipment, and shop expenses are allocated on a car-mile basis. The total costs for the years studied are shown in Table I.

Table I

Cars	Car Miles	Cents per Car Mile
PCC	32,500,000	1.70
Old-type cars	60,912,000	2.49

These data show a net saving of 0.79 cents per car mile for the PCC cars, which excludes painting, since none had been through the paint shop. A liberal allowance for this item reduces the net saving to 0.7 cents per car mile or a gain of 28 per cent.

Track- and roadway-maintenance costs have been substantially reduced by PCC car operations. The extensive use of rubber and the light weight of the trucks of the new cars are two pertinent factors affecting the action upon track and roadway in comparison with older-type equipment. The principal use of rubber is in the wheels and journal springs. The resilient construction permits normal deflections of one-fourth inch for wheels and two inches for journal springs. The Presidents' Conference Committee directed a program of exhaustive impact tests to determine comparable vertical accelerations of resilient and conventional steel wheels. Based on these data, the earlier-type cars in Pittsburgh impart impact forces to the track structure six times as great as PCC cars incident to track irregularities and vehicle movement. Track damage because of this action loosens the entire structure including the paving bond. Surface water enters, resulting in rapid deterioration and expensive maintenance. Special track work is particularly vulnerable because of the frogs and switches.

The lower impact forces and lighter weight of PCC cars permit a less expensive track structure. With the older-type cars a nine-inch girder rail weighing 134 pounds per yard, wood ties, ballast, and blockstone paving were required at a cost of $12.50 per foot. In the case of PCC cars, it is feasible to use $5^3/_4$-inch Tee rails weighing 100 pounds per yard, steel ties and concrete at a cost of $9 per foot. Experience has shown that track life can be stepped up from 25 to 32 years by the modern cars. There are 394 miles of paved track. The annual reconstruction requirement is 15.8 and 12.3 miles, respectively, for a 25- and 32-year life, a saving of 44 per cent. The system has 54 miles of open track on which there is a reduction of 13 per cent in reconstruction costs. Special work life is lengthened by the use of modern cars. The indicated improvement is 10 years (20- to 30-year life), because the impact pounding is reduced to one-sixth. The special work maintenance has dropped 14 per cent with 301 PCC cars in operation. With 100 per cent PCC cars on the system, the betterment will be 33 per cent. Ordinary track maintenance includes heavy repairs. Such work costs $1 per foot and the schedule has been seven miles per year. An allowance of 2.5 miles per year has proved ample which saves 12 per cent.

During the year ending June 30, 1941, 29,481,000 car miles were operated, with 16,923,000 or 57.3 per cent PCC. A summary of the percentages saved in track and roadway expense is shown in Table II.

Table II

PCC Cars in Use	Per Cent 100	 80	 57
Savings			
Reconstruction—paved track	44.0	33.2	25.1
Reconstruction—open track	13.4	10.7	7.6
Special work	33.3	26.6	19.0
Ordinary maintenance	12.0	9.6	6.9
Average	37.1	29.6	21.1

PCC cars have reduced accidents because of their many safety features and quick stopping ability. Analyses of four comparative 12-month periods show decreases of 24, 25, 25, and 26 per cent of accidents in comparison with earlier-type cars. A typical illustration is the accompanying data for the year ending September 30, 1940 shown in Table III.

Table III

	Accidents	Car Miles	Accidents Per 10,000 Car Miles
Earlier-type cars	5,166	14,729,129	3.51
PCC Cars	3,531	13,346,580	2.65
Reduction in accidents per car mile with PCC cars			0.86
Per cent reduction in accidents per car mile with PCC cars			25

The average accident rate for the four years from 1932 through 1936, prior to any PCC car operation, was 2.74 per 10,000 car miles. The 25 per cent reduction obtained with PCC car operation means 0.69 less accidents per 10,000 car miles. Thus, the reduction is 1,167 accidents for 16,923,000 PCC car miles operated during the year ending June 30, 1941. An analysis of the savings incident to the elimination of these accidents is shown in Table IV.

Table IV

Per cent total accidents resulting in nonlitigated settlement	15.97
Per cent total accidents resulting in litigated settlement	4.66
Saving in nonlitigated settlements (1,167 × 0.1597)	186
Saving in litigated settlements (1,167 × 0.0466)	54

PCC cars provide faster schedule speeds than the earlier types because of their more rapid starting and stopping characteristics, higher free-running speeds, and quicker interchange of passengers. This makes it practicable to give the same frequency of service with fewer units in the case of the modern vehicles. A tabulation is included in Table VI which shows improvements made in schedules with high-speed cars and the first 201 PCC cars. In comparison with the earlier low-speed types the average betterment is 16 and 12 per cent, respectively, for base and peak service.

Considerable improvement was realized when the earlier-type cars were speeded up which meant less schedule speed increase for the system with the introduction of PCC cars. Also, the speeded-up cars are used with PCC cars in peak service, which limits the schedule speeds obtainable with the latter vehicles. A study of the routes on which the 301 PCC cars are now operating shows that improved performance permits replacing 10 of the earlier-type cars with 9 of the new type—a gain of approximately 10 per cent.

Table V

Year	Routes Using PCC Cars (18-Hr Base Service) Index	Routes Using Old-Type Cars Index	Spread Between Two Classes of Routes
1933	100.00	100.00	
1934	104.37	107.09	−2.72
1935	104.94	106.95	−2.01
1936	114.56	114.27	0.29
1937	117.65	112.14	Transition period
Year ending 3/31/39	109.66	102.97	6.69
1939	108.58	103.63	4.95
1940	112.78	104.95	7.83

Average spread in 1934, '35, and '36 =

$$\frac{-2.72-2.01+0.29}{3}=-1.48$$

Average spread in last 3 periods =

$$\frac{6.69+4.95+7.83}{3}=6.49$$

Total average spread =7.97%

The increase in schedule speed incident to the use of PCC cars has a direct bearing on the betterment of gross revenues. Performance is highly important and contributes more to attracting passengers than any other feature. Of course, the comfort of the vehicle is of consequence from the "satisfied-customer" viewpoint. The first 201 PCC cars placed in service have been operating a sufficient length of time to show a definite "revenue trend" for the new equipment.

An analysis has been made which indicates the results shown in Table V.

The year ending March 31, 1939, was the first complete 12 months after all of the routes using the 201 new vehicles had been equipped for operation of the 18-hour base service. The gain in revenue shows a spread of practically eight per cent in favor of PCC cars.

PCC Cars Extensively Used

The PCC car is an outstanding city-transit vehicle with exceptional utility. Its success for improving urban travel is demonstrated by the favorable results obtained in Pittsburgh and the 15 other cities which use the vehicle. It serves to reduce car maintenance, lower the upkeep of track and roadway, decrease accidents, raise schedule speeds, and increase revenue. A degree of excellence is established in public travel that is a creditable contribution to the advancement of city transit.

Table VI. Pittsburgh Railways Company Data on Schedule-Speed Improvements With High-Speed and PCC Cars

Route	Miles Round Trip	Low-Speed Cars				High-Speed Cars						PCC Cars					
		Min. Round Trip		Schedule Speed—MPH		Min. Round Trip		Schedule Speed—MPH		% Schedule Speed Increase		Min. Round Trip		Schedule Speed—MPH		% Schedule Speed Increase	
		Base	Peak	Base	Peak	Base	Peak	Base	Peak	Base	Peak	Base	Peak	Base	Peak	Base	Peak
2—Etna	12.48	57	60	13.10	12.50	50	53	15.00	14.10	14.50	12.80	46	54	16.30	13.90	24.40	11.20
3—Millvale	10.03	57	60	10.60	10.00	45	49	13.40	12.30	26.40	23.00	44	52	13.70	11.60	29.20	16.00
4—Troy Hill	5.47	38	41	8.60	8.00	33	35	9.95	9.40	15.70	17.50						
6—Brighton Road	9.49	50	56	11.40	10.20							45	51	12.70	11.20	11.40	9.80
7—Charles Street	6.34	47	50	8.10	7.60	45	48	8.45	7.90	4.30	3.95						
8—Perrysville	10.41	49	55	12.70	11.40	50	53	12.50	11.80	−1.58	3.50						
10—West View	14.36	69	72	12.50	12.00							58	68	14.85	12.70	18.80	5.80
13 & 14—Emsworth & Avalon	17.85	82	86	12.90	12.30							72	80	14.70	13.20	14.00	7.30
15—Bellevue	16.25	79	82	12.30	11.90							69	76	14.10	12.90	14.60	8.40
18—Woods Run	8.81	50	54	10.60	9.80	49	54	10.80	9.80	1.90	0						
19—Western	5.96	35	37	10.20	9.70	34	39	10.50	9.20	2.90	−5.20						
27—Carnegie	14.57	71	74	12.30	11.80	62	67	14.10	13.00	14.60	10.20	62	68	14.10	12.80	14.60	8.50
30 & 31—Crafton	8.86		41		13.00		39		13.60		4.60						
30 & 31—Sheridan	9.60	48		12.00		42		13.40		11.70							
30 & 31—Ingram	11.69	56	59	12.50	11.90	52	56	13.50	12.50	8.00	5.00						
38—Mt. Lebanon	11.21	61	65	11.00	10.30	51	57	13.20	11.80	20.00	14.60	52	53	12.90	12.70	17.30	23.30
39—Brookline	10.06	56	59	10.80	10.20	48	49	12.60	12.30	16.70	20.60						
40—Mt. Washington	8.84	57	70	9.30	7.60	50	55	10.60	9.70	14.00	27.60						
42—Dormont	10.06	56	59	10.80	10.20	44	48	13.70	12.60	26.90	23.60						
47—Carrick T.	12.52		71		10.60		66		11.40		7.50						
50—Carson St.	7.05	49	52	8.60	8.10	44		9.60		11.60		44	48	9.60	8.80	11.60	8.60
53—Carrick	12.12	69	74	10.50	9.80	55		13.20		25.70		59	69	12.30	10.50	17.20	7.20
55—Homestead, B. & E.P.	25.28	128	138	11.80	11.90	112	116	13.50	13.70	14.40	19.10	112	124	13.50	12.20	14.40	10.90
56—McKeesport (McK.)	26.40	124	129	12.80	12.30												
56—McKeesport (P)	25.72					114	119	13.50	13.00								
57—Glenwood	9.70	44	48	13.20	12.10	41	46	14.20	12.70	7.60	5.00						
58—Greenfield	9.76	52	55	11.30	10.60	43	49	13.60	12.00	20.40	13.20						
60—E. Liberty & H.M.	11.35	67	69	10.20	9.90	61	63	11.20	10.80	9.80	9.10						
60—E. Liberty & H.R.	13.69					69	73	11.90	11.20								
64—Wilkinsburg & E. Pgh.	25.40	138	141	11.00	10.80	113	123	13.50	12.40	22.70	14.80	120	130	12.70	11.70	15.50	8.30
66—Wilkinsburg	15.20	81	84	11.30	10.80	74	79	12.30	11.60	8.90	7.40						
67—Swissvale R.	18.27	94	98	11.70	11.20	78	83	14.10	13.20	20.50	17.80						
67—Swissvale 13th	20.41											98	102	12.50	12.00		
68—McKeesport 6th	30.15	165	165	11.00	11.00	144	154	12.60	11.70	14.50	6.40	135	138	13.40	13.10	21.80	19.10
68—McKeesport P.	32.43											149	152	13.10	12.80		
69—Squirrel Hill	12.36	66	69	11.20	10.70	62	65	12.00	11.40	7.10	6.50	64	68	11.60	10.90	3.60	1.90
71—Negley	13.26	78	81	10.20	9.80	60	66	13.30	12.00	30.40	22.40	62	73	12.80	10.90	25.50	11.20
73—Highland	13.52	76	80	10.70	10.10	62	65	13.10	12.50	22.40	23.80	62	72	13.10	11.30	22.40	11.90
75—Oak. & E.L. & Wilkinsburg	14.81	86	91	10.30	9.80	70	75	12.70	11.90	23.30	21.40	71	83	12.50	10.70	21.40	9.20
76—Hamilton	15.74	85	91	11.10	10.40	71	75	13.30	12.60	19.80	21.20	70	79	13.50	12.00	21.60	15.40
77 & 54—N.S. & Carrick	24.43	136		10.80		122	135	12.00	10.90	11.10							
82—Lincoln	14.44	89	96	9.70	9.00							81	84	10.70	10.30	10.30	14.50
87—Ardmore	28.80	150		11.50		137	146	12.60	11.80	9.60							
87—Ardmore S.	2.20	12	13	11.00	10.20	14	17	12.50	10.30								
88—Frankstown	14.76	83		10.70		75	80	11.80	11.10	10.30		74	78	12.00	11.40	12.20	
88—Frankstown S.	1.84	10	11	11.00	10.00	8	9	13.80	12.30			14	15	11.70	11.00		
94—Aspinwall	14.82	82	87	10.80	10.20							72	77	12.40	11.50	14.80	12.70
96—E.L. & 62nd	9.31	54	54	10.40	10.40	50	50	11.20	11.20	7.70	7.70						

Improvement in Modern Meter-Testing Technique

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THROUGH more than a score of years a variety of devices has been developed to simplify the procedure and to reduce the hazard of personal errors in the technique of watt-hour meter testing. Of these devices the stroboscope and the photoelectric timer have already become generally accepted tools of the meter-testing art. Indeed, meter-testing problems differ somewhat in the field, in the laboratory, and in the factory, and consequently, no single device nor combination of devices can be considered ideally applicable to all three forms of testing. However, in each case much is to be gained through the use in some form of the many schemes[1,2] which have recently been developed. Such schemes apply equally to single-phase and polyphase meter testing, though in this discussion we shall be concerned only with the single-phase watt-hour meter and the testing problems peculiar to the factory. Briefly these problems are:

1. Removal of the hazard of personal error in testing.
2. Maintenance of closer control of quality.
3. Elimination of errors in portable standards through handling.
4. Reduction of skill required in testing.
5. Shortening of testing time.
6. Reduction of handling of meters.
7. Elimination of storage space for meters awaiting test.
8. Maintenance of a uniform flow of the product from assembly through to packing.

All of these factors contribute to the achievement of greater uniformity and calibration accuracy as well as to a generally improved quality of the product.

The system which was evolved and which is described in this paper is the result of an investigation conducted under actual manufacturing conditions. A small production line was set up which afforded ready means for close study of the factors outlined above and for trial of the several schemes investigated. It became immediately apparent that testing should become as nearly as possible an integral part of the assembly procedure, if the necessity for storing meters for test were to be avoided. That is, the testing procedure must be broken down into operations that might readily be accomplished during or immediately after the assembly of the meter and in synchronism with the cycle of assembly operation. Logically then, the testing procedure must be broken down into these two major steps:

1. Adjustment of calibration at each of the three conventional load points (100 per cent load, unity power factor; 10 per cent load, unity power factor; 100 per cent load, lagging power factor 50 per cent).
2. Verification of calibration accuracy at all three load points.

Conventionally, calibration adjustments at these three load points are made in successive cut and try steps. Usually a rough adjustment of light load is made by applying potential only to the meter being tested and adjusting until the meter disk does not creep. This is the familiar "creep method." Lag-load adjustment, because of the uniformity of controlled manufacturing processes, is set within close limits in assembly. The setting of full load or the location of the damping magnet with respect to the center of the meter disk is done by successive test runs and adjustments until the desired accuracy is achieved. When the accuracy at all three load points called "as found" readings are obtained, a simple calculation enables the tester to make those minor adjustments which will bring the meter accuracy at all points within the desired limits. Obviously, the smaller the spread in the "as found" readings the simpler the calculation and adjustment required on a particular meter. However, many cut and try steps are required before the "as found" readings are within sufficiently small limits to permit a single set of adjustments which will bring the entire calibration within the accuracy required.

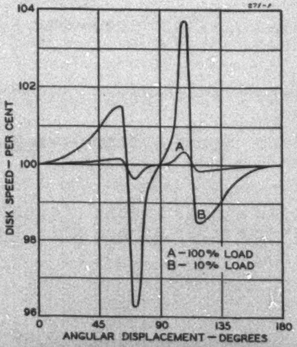

Figure 1. Curve showing angular velocity of watt-hour meter disk as a function of displacement

Speed is given in per cent of nominal-load value for a General Electric type I-30 watt-hour meter

In addition to the above limitations of the conventional procedure, long-time test intervals must be taken for each test run if a high degree of accuracy is to be achieved. This is due to the fact that meters are usually tested in gangs of as many as 15, connected to a common load with a rotating standard, for an interval corresponding to a given whole number of revolutions of the rotating standard. The displacement from the starting position of the disk of each meter is a measure of the error at the particular load point being tested. The precision within which these readings of accuracy can be made depends on the length of the test interval. Further, there is the problem of correcting for the error in the rotating standard.

Thus it is apparent, that much manipulation is required, before a satisfactory set of "as found" readings is obtained. It is also evident that, if the preliminary steps could be accomplished less laboriously, and if the entire calibration accuracy could be had at once, the procedure would be tremendously simplified. The discussion which follows, therefore, will describe the usefulness of the devices developed in this investigation to accomplish the desired simplification in procedure.

Calibration Adjustment

FULL-LOAD ADJUSTMENT

Consistent with the scope of the investigation, the means provided for making calibration adjustment must be simple and, if possible, render instantaneous indication of changes in calibration. The stroboscope[3,4] is immediately suggested. We must, however, consider its limitations, for its practical value is dependent upon the angular velocity of the object being viewed, a distinct image, and a fre-

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quency of light impulses which is compatible with good vision. The last two of these requirements, over the common ranges of frequency required, are readily met by mechanical or electronic means, or by a combination of both.

The third requirement deals with the angular velocity of the moving element, and an analysis of the conditions required for a stationary image or for a "hunting" image is necessary to understand its influence. Assuming a constant rate of light flashes upon a moving element, any change in angular velocity of this element will produce a change in the rate of displacement of the stroboscopic image with respect to a fixed reference point. When synchronism is achieved a stationary image results.

At synchronism $f = \frac{Nw}{2\pi}$

where

f = rate of light flashes in cycles per second

w = angular velocity of disk in radians per second

N = number of equally spaced elements

If the disk velocity is alternately greater and less than the synchronous speed, the image will be seen to advance and recede according to fluctuations in velocity. This characteristic is called "hunting." It is largely this condition which limits the universal application of the stroboscope in watt-hour meter adjustment, since the operation becomes tedious and time consuming and, in cases of excessive hunting, requires a high degree of skill to achieve accuracy. If observation of image displacement is hindered by hunting, the fluctuating image must be followed through its entire excursion (advance and recession) and the resultant advance and recession noted.

Figure 1 shows the speed–displacement characteristic of a watt-hour meter at both full load and ten per cent load. The magnitude of the variations in this curve is constant at all values of load, so that in percentage this magnitude decreases with increased load. The speed characteristic of modern meters is, therefore, quite uniform at the high values of load, but at light loads a large variation in angular velocity exists.

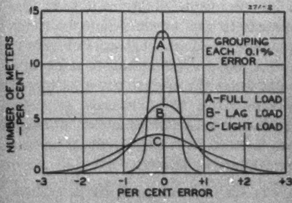

Figure 2. Distribution curves of calibration errors before final adjustment

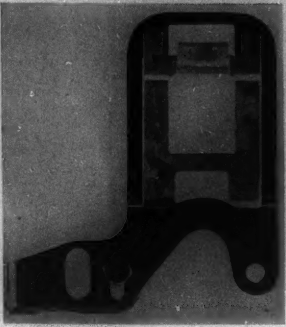

Figure 3. Adjustable-type compensated lag plate

The hunting of the image at these light loads could be eliminated if the flashing rate were made to fluctuate at a frequency corresponding to the irregular angular velocity of the disk and kept in time phase. This condition can be actually achieved only with difficulty and it is more practical to utilize a uniform flashing rate.

With the above facts in mind, a quantitative consideration of the angular velocities of the disk at various values of load on the meter, together with the desired limits of accuracy at each value of load, will define conclusively the limits of stroboscopic application to meter testing. A modern watt-hour meter such as that from which the characteristics were determined for Figure 1 has 720 equally spaced elements on the edge of the disk. At nominal load its speed is 1.745 radians per second, and it would, therefore, require a flashing rate of 200 per second to synchronize it at this speed or 20 per second at one-tenth this speed.

The image advance of this meter will be 7.2 elements per revolution of the disk when 99 per cent accuracy is obtained, or 0.72 element per revolution when 99.9 per cent accuracy exists. These displacements can be instantaneously observed if there is no hunting and may not be imperceptible with a small amount of hunting. If, however, hunting produces an image fluctuation greater than about five times the image displacement caused by the inaccuracy in meter calibration, then the personal error becomes large, and the stroboscope cannot be used except by specially skilled workers. This value is arbitrary and is the opinion of the authors obtained through experience.

The hunting magnitude is influenced by various factors which can be conveniently classified as follows:

I. Those that produce an essentially constant hunting magnitude at all loads:

(*a*). Eccentricity of the disk.

(*b*). Nonuniformity of flashing rate.

(*c*). Nonuniformity of disk marking.

(*d*). Nonhomogeneity of the disk metal.

(*e*). Bent disks.

II. Those that produce their maximum effect at low loads:

(*a*). Variation in potential torque with disk position.

(*b*). Variation in friction with disk position.

Since the maximum total hunting will be produced at lower loads, we can see not only the difficulty of adjusting with small torques such as at light loads, but also the advantage of adjusting for nominal-load calibration with 200 per cent load current. An analysis of tests has shown that nominal-load adjustment can be made at 200 per cent load without affecting the load-speed characteristics of the meter appreciably, and for this reason, the fixture to be described later is designed to operate at 200 per cent load.

Figure 4. Front view of section of conveyor system and calibration-adjusting stations

Light-Load Adjustment

It can be concluded from the foregoing analysis that the stroboscope does not lend itself satisfactorily to calibration adjustment at light loads. Its convenience is also lessened, due to the necessity of alternately and repeatedly adjusting each load until all errors are sufficiently small. This is due to the effect of the light-load adjustment on other loads.

To be most convenient, the light-load adjustment must be accomplished as nearly as possible without influencing the other load adjustments, and the well known "creep" method which adjusts the torque due to potential alone so that the disk will not creep makes this possible.

The method consists of applying voltage to the potential coil and, without any load current, moving the light-load adjustment until the meter disk will not creep in either direction. In principle, it is one used for many years as a rough method of adjusting meters at light load.

Let us analyze just what we accomplish by so doing, for an analysis will show that although we are not fundamentally getting the adjustment we would like, we nevertheless accomplish a very good approximation of light-load adjustment.

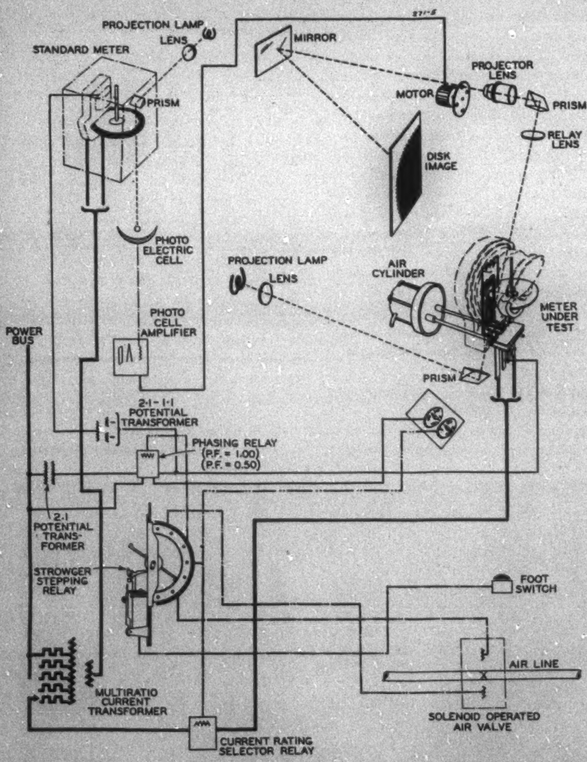
PROJECTION LAMP
LENS
STANDARD METER
PRISM
MIRROR
PROJECTOR LENS
MOTOR
PRISM
RELAY LENS
DISK IMAGE
PHOTO ELECTRIC CELL
PROJECTION LAMP
LENS
AIR CYLINDER
METER UNDER TEST
PHOTO CELL AMPLIFIER
POWER BUS
PRISM
2-1-1-1 POTENTIAL TRANSFORMER
PHASING RELAY (P.F. = 1.00) (P.F. = 0.50)
2-1 POTENTIAL TRANS-FORMER
STROWGER STEPPING RELAY
FOOT SWITCH
AIR LINE
MULTIRATIO CURRENT TRANSFORMER
SOLENOID OPERATED AIR VALVE
CURRENT RATING SELECTOR RELAY

Figure 5. Line schematic diagram of internal connections in adjusting station

Figure 6. Portion of conveyor system showing bank of verification stations

A definite but small error is introduced when we attempt to make the potential torque, T_e, equal to zero for, in a meter correctly calibrated at full, light, and lag loads, we find that T_e is rarely zero.[5] This small error will be a variable influenced by two factors shown below.

If we consider a general equation, such as the following, and realize that at the moment of "no creep" with no motion of the disk, the inertia and damping terms (both dependent on disk motion) are not present, and since the balance is performed with no load current, the equation[6] reduces from

$$J\frac{d^2\theta}{dt^2}+(D_m+D_t+D_e)\frac{d\theta}{dt}+(T_{et}+T_t+T_e-T_f)=0$$

to

$$T_f=T_e$$

which means that the driving torque due to potential flux alone is made equal to the friction torque (see appendix).

This simple equation is complicated by the fact that T_e (similar to Figure 1) is not a simple function but depends on the disk displacement. Therefore, our calibration will depend on what part of the curve T_e we make equal to the friction torque.

By using care to see that the anticreep holes are kept 90 degrees from the element air gap, we can adjust the meter at a disk displacement shown in Figure 1, as 0 or 180, thus attempting to adjust at a point where the average potential torque is zero. This will obviously give the greatest consistency possible, although two factors will tend away from extremely precise results:

1. T_f, the friction torque, can be positive or negative depending on the direction of the tendency to creep.

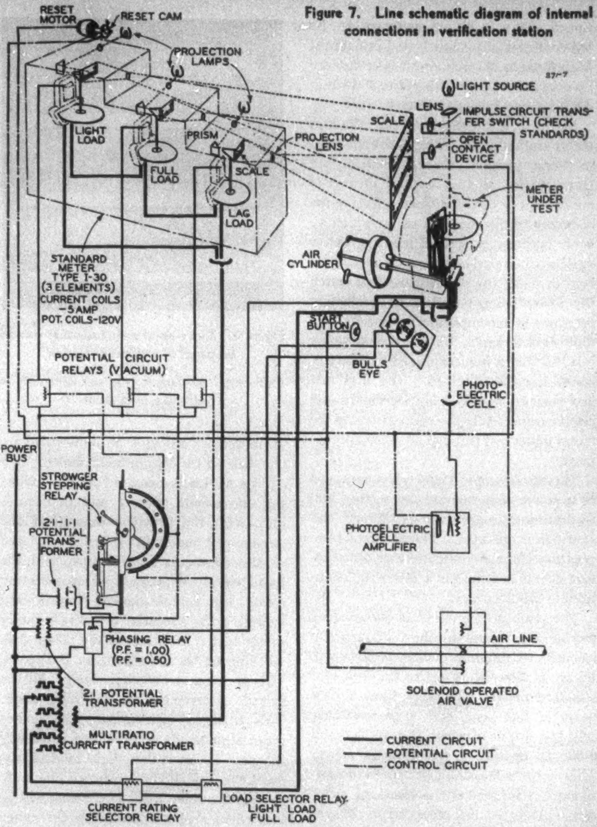

Figure 7. Line schematic diagram of internal connections in verification station

RESET MOTOR
RESET CAM
PROJECTION LAMPS
27-7
LIGHT SOURCE
LENS
IMPULSE CIRCUIT TRANSFER SWITCH (CHECK STANDARDS)
SCALE
OPEN CONTACT DEVICE
LIGHT LOAD
PRISM
PROJECTION LENS
FULL LOAD
SCALE
METER UNDER TEST
LAG LOAD
AIR CYLINDER
STANDARD METER TYPE I-30 (3 ELEMENTS) CURRENT COILS -5 AMP POT. COILS-120V
START BUTTON
BULLS EYE
POTENTIAL CIRCUIT RELAYS (VACUUM)
PHOTO-ELECTRIC CELL
POWER BUS
STROWGER STEPPING RELAY
2-1-1-1 POTENTIAL TRANSFORMER
PHOTOELECTRIC CELL AMPLIFIER
PHASING RELAY (P.F. = 1.00) (P.F. = 0.50)
AIR LINE
2:1 POTENTIAL TRANSFORMER
SOLENOID OPERATED AIR VALVE
MULTIRATIO CURRENT TRANSFORMER
CURRENT CIRCUIT
POTENTIAL CIRCUIT
CONTROL CIRCUIT
CURRENT RATING SELECTOR RELAY
LOAD SELECTOR RELAY LIGHT LOAD FULL LOAD

2. The point of adjustment may differ from that at which the shunt torque has its average values.

Although the above discussion might indicate that this method will not inherently give absolutely correct adjustment, a practical application of it has indicated that a very high percentage of meters will be obtained which are within normal allowable tolerance. A curve showing the distribution of accuracies over a large number of meters tested is shown in Figure 2 and is not inconsistent with the above analysis. For a preliminary adjustment there seems to be no better method of setting the light-load adjustment.

Any effort to try to adjust to a slight forward creep to compensate for the average negative error is apt to result in "personal" errors based on "judgment" which will be greater than the above inherent error, and, therefore, it is to be discouraged.

LAG-LOAD ADJUSTMENT

The third load point at which calibration is made consists of setting the lag plate which compensates for the phase angle of the potential coil. Adjustment of this compensator is made at 50 per cent power factor and 100 per cent load current. From the preceding discussion of the stroboscope, it is apparent that reasonable accuracy may be had by using that device for making adjustment. Other methods, such as creep at zero power factor, torque method, or rotating-standard method, are also suitable, and at this juncture, the question naturally arises, why not use one of these methods—for instance, the stroboscope? This might well be accomplished, and many modifications have been developed which make the stroboscopic method convenient. However, any such scheme would require a succession of tests at each of the three loads and a return, until the error at each point has become sufficiently small to to give satisfactory calibration. In our application a "straight-line" procedure was sought, and since such a method involves definite advantages, any one which was not directly applicable to such a straight-line procedure was considered undesirable.

Adjusting the meter not to creep with zero power factor might well be used to accomplish this purpose, but it was found that, with meters equipped with an adjustable lag plate as shown in Figure 3, a quicker and equally satisfactory method is to set the position of the lag plate mechanically.

When this is done with an accurate fixture, a large percentage of meters will be correctly calibrated for lagging loads. A distribution curve of accuracies to be expected are shown in Figure 2b, which summarizes the experimental results on hundreds of meters over a period of several months. Rigid manufacturing control is necessary of the factors entering into the amount of lagging required and that obtained, such as the resistance of the potential coil and the conductivity of the lag plate, and so forth.

A micrometer fixture which can be adjusted from time to time to compensate for the drift due to manufacturing tolerance is an advantage, but for the highest degrees of accuracy, a slight further adjustment will be necessary on a small percentage of the meters.

A device in which this final adjustment can be conveniently made will be described later, but at this point it may be well to indicate the steps necessary to obtain this high degree of accuracy and to show how they fit into the manufacturing procedure.

I. While subassemblies are being made:

(*a*). Set lag plate mechanically.

II. After assembly:

(*b*). Set light-load adjustment by creep method.

(*c*). Set full-load adjustment stroboscopically.

III. After preliminary adjustment:

(*d*). Check calibration at all three loads.

(*e*). If necessary, improve light- and lag-load accuracies.

(*f*). Recheck calibration at all three loads.

Adjusting Device

For best results, adequate equipment must be used for the various operations. A very satisfactory fixture for accomplishing the preliminary adjustments mentioned above is shown in figure 4. Its schematic circuit is illustrated in figure 5. Maintenance on such a unit is minimized by conveniently mounting it to slide in or out of a conventional filing cabinet.

Figure 5 illustrates how the standard meter and meter under test are connected in "series-parallel" through multitapped transformers, so that various ratings of meters can be tested without affecting the accuracy of the standard meter which always operates at a fixed load. Selector switches mounted on the panel operate relays to select the desired loads to match the meter being tested, and the testing sequence is accomplished by means of a foot switch which operates a stepping relay so that a predetermined sequence of connections is obtained. The first and last position in this sequence makes and breaks respectively the connections to the meter under test through air-pressure contacts.

The stroboscopic standard is arranged in a convenient manner for setting and maintaining its accuracy at the one load at which it operates. Thermostatic temperature control eliminates any temperature effects in it and is a factor in maintaining this accuracy.

The stroboscopic image is obtained by mechanical means so that a maximum intensity of illumination may be obtained. Figure 5 shows slots cut in the disk of a standard meter. Through these slots a beam of light energizes a photoelectric cell, the output of which is amplified and made to operate a synchronous motor. This motor will operate at a speed proportional to the speed of the standard meter disk. A slotted disk mounted on the synchronous motor shaft interrupts another beam of light which through a suitable optical system projects an image of the teeth on the edge of the disk onto a screen. The standard meter is connected to operate continuously so that when creep adjustment is made, the image, though not stroboscopic, will not be obliterated by the disk on the synchronous motor.

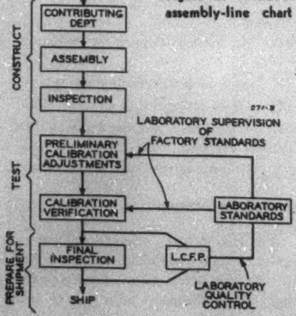

Figure 8. Product assembly-line chart

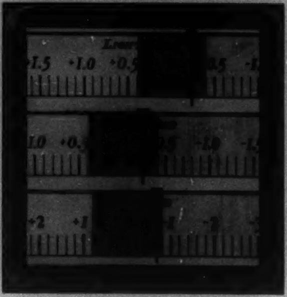

Figure 9. Close-up of scale images as viewed on panel of verification station

Calibration accuracy is read simultaneously at all three load points

The device is operated in the following convenient manner and in accordance with the previous theoretical discussion. A meter to be tested is placed in the contact-making fixture, and the foot switch is depressed. This operation closes the contact-making fixture and energizes the potential coil of the meter under test. An image of the meter disk will appear on the screen on the panel. This is not a stroboscopic image, but an actual enlargement of the disk teeth. This enlarged view of the disk makes creep adjustment of light load extremely simple. Before making light-load adjustment the disk should be turned manually so that the creep holes in the disk will be located in a plane parallel to the meter element. Then the light-load adjustment may be set so that no motion of the disk is observed on the screen.

The next depression of the foot switch energizes the current circuit of the meter under test, and there will appear a stroboscopic image on the screen. The position of the damping magnet is now set so that the image displacement is equal in both directions, if a slight amount of hunting is present. Having made this full load adjustment, another pressure on the foot switch will de-energize the meter under test and release the pressure on the contact block. The meter is then removed to the verification device where a picture of the calibration accuracy is obtained.

Verification of Calibration

Though for many years precise methods of watt-hour meter calibration have made use of precision instruments and constant load, it has been shown[7] that greater reliability is achieved by means of standard watt-hour meters giving for the first time the standard of watt-hour units. This standard, though maintained in the laboratory, can be readily transferred to the factory, and control maintained. Thus, it was decided to adopt the rotating-standard comparison method for the verification device. Essentially, the device consists of suitable switching and revolution-counting mechanism, together with the familiar portable standard watt-hour meter. The design and arrangement of the apparatus, however, are such as to achieve the results expressed in our introduction, namely, to obtain a direct indication of the complete calibration accuracy of the meter, and, in this respect, we believe that it is unique. The description which follows will deal chiefly with the novel design features and flexibility of the device.

Verification Device

The unit shown in Figure 6 in a multiple group was designed to give simultaneous indication of the complete meter-calibration accuracy. It employs a photoelectric timer which makes its operation automatic. The measurement is made by comparing the speed of the meter under test at each load with the speed of a separate standard for each value of load. This comparison follows automatically in sequence so that, at the end of a given time, the entire calibration appears upon the screen. The line diagram, Figure 7, shows schematically the arrangement of the apparatus by means of which this verification is made. In this figure, a light beam is directed from above the meter under test, through the path of the anti-creep holes and onto a photoelectric cell. The amplified output of the photoelectric cell energizes a sensitive relay. This arrangement is such that for each half revolution of the meter disk having two anti-creep holes, the above relay will operate once. The operation of this relay actuates a stronger stepping relay which in turn energizes relays to perform the following operations in the sequence indicated.

As the starting button is pressed with a meter in position, the standard meters are reset to zero through motor-driven cams, and the meter under test is energized. With the first light impulse after reset, light load is thrown on the meter under test, and the next impulse energizes the light-load standard. After a predetermined number of revolutions of the meter under test, the stepping relay has advanced to a contact that stops the light-load standard and throws full load on the meter being tested. The next impulse starts the full-load standard which runs until stopped by the stepping relay after a fixed number of revolutions of the meter undergoing test. As the full-load standard is stopped, lag load is thrown on the meter and the standard runs until stopped by the stepping relay. This again occurs after a given number of revolutions of the meter which energizes the photoelectric cell.

After these operations a picture of the meter accuracy at full load, light load, and lag load appears magnified in full view on a screen. This picture is obtained by projecting and focusing the images of transparent indicators which rotate with the standards and which replace the pointers for indicating the displacement of the pointer from zero. The transparent indicators are marked in divisions to represent error in tenths of a per cent and a manually operated index can be set to the point at which the zero position of the projected indicator image should stop if there is no error in the meter under test.

Once the accuracy of the meter has been visualized, the apparatus is ready to function again, or a release button can be pressed to disconnect the meter making it ready to remove the checked meter and replace it with one to be verified.

Conclusions

It will be readily observed that a suitable arrangement of the devices described when operated in the sequence discussed will result in an operating system which permits a uniform and progressive testing routine, readily coupled to conveyorized assembly manufacture. Figure 8 shows how the parts coming from contributing departments can be assembled and adjusted in a simple, uniform routine which moves along in progressive steps until the meter is ready for shipment, and which allows a laboratory check of the product to be made on sufficient quantities and at definite intervals without inconveniencing or upsetting the normal advance of these conveyorized operations. Although less skill is required by the operators, due to less tedious routine and more mechanized operations, more consistent accuracy can be expected, as errors are easily visible on an enlarged scale, and personal errors in judgment are eliminated.

The operation of this straight-line setup will, of necessity, reduce inventories of meters awaiting test and insure better service, since when a meter starts through there is little chance of sidetracking it, unless defects are found during inspection. This improved service means more meters delivered in quicker time and with full assurance of the high quality which any meter organization would be proud to associate with their own work.

Appendix

$$J\frac{d^2\theta}{dt^2}+(D_m+D_i+D_e)\frac{d\theta}{dt}-(T_{ei}+T_i+T_e-T_f)=0$$

where

J = Inertia of moving element (gram millimeters per second per second)
θ = Angular displacement of disk (radians)
t = Time (seconds)
D_m = Damping factor relating to permanent magnet (gram millimeters per second)

$$D_m=\frac{st}{g}G_m(\phi_m)^2 10^{-8}$$

D_i = Damping factor relating to current flux (gram millimeters per second)

$$D_i=\frac{st}{g}G_i(\phi_i)^2 10^{-8}$$

D_e = Damping factor relating to potential flux (gram millimeters per second)

$$D_e=\frac{st}{g}G_e(\phi_e)^2 10^{-8}$$

T_{ei} = Driving torque due to interaction of current and potential flux (gram millimeters)

$$T_{ei}=4\pi\frac{stf}{g}G_d\phi_e\phi_i\cos\mu\ 10^{-8}$$

T_i = Driving torque due to load current flux (gram millimeters)

$$T_i=4\pi\frac{stf}{g}G_{di}\phi_i^2\cos\mu_i\ 10^{-8}$$

T_e = Driving torque due to potential flux (gram millimeters)

$$T_e=4\frac{stf}{g}G_{de}\phi_e^2\cos\mu_e\ 10^{-8}=f(\theta)+K$$

T_f = Friction torque (gram millimeters)
$f(\theta)$ = Anti-creep torque (a function of disk rotation in gram millimeters with an average value of zero)
K = The constant component of T_e which when added to $f(\theta)$ gives T_e
s = Conductivity of disk (mhos per centimeter cube)
t = Thickness of disk (centimeters)
g = Gravitational constant (centimeters per second per second)
G_m = Damping constant relating to permanent magnet (dimensionless)
G_i = Damping constant of load current flux (dimensionless)
G_e = Damping constant of potential flux (dimensionless)
G_d = Driving constant relating to current and potential flux (dimensionless)
G_{di} = Driving constant relating to current flux (dimensionless)
G_{de} = Driving constant relating to potential flux (dimensionless)
ϕ_m = Useful permanent magnet flux (Maxwells)
ϕ_i = Useful load current flux (Maxwells, effective value)
ϕ_e = Useful potential flux (Maxwells, effective value)
f = Frequency (cycles per second)
μ = Angle between ϕ_i and ϕ_e (radians)
μ_i = Equivalent angle of components of ϕ_i which produce torque T_i
μ_e = Equivalent angle of components of ϕ_e which produce torque T_e

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Single-Phase A-C Electric Locomotives on the Pennsylvania Railroad—Protection and Tonnage Rating

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MEMBER AIEE

THE single-phase electric locomotive is thought of quite generally as a piece of motive power for heavy traction service. To an electrical engineer, however, it consists fundamentally of electric motors geared to wheels with means for controlling the applied voltage to handle a variable load at varying speeds.

From this point of view it is interesting to consider the protective features of the motors and their associated electrical apparatus to develop devices and methods which are necessary to guard against the hazards common to the operation of all electrical devices and those special to an electric locomotive. This paper will consider these features as they apply to the electric locomotives operating on the Pennsylvania Railroad.

The Electrical Apparatus

The single-phase a-c locomotive with series motors receives its power from a trolley at a relatively high voltage in order to provide economical distribution and good voltage regulation. The commutator-type motors must operate at a relatively low voltage in order to keep the commutators within a reasonable size because of the limitation of volts between bars.[1] A transformer is, therefore, necessary. Having the transformer to provide the lower voltage, a method for tap changing under load can then be introduced to control the voltage applied to the motors. The motors are permanently connected in parallel groups with the motors in each group in series. This permits a motor group to be cut out of service with no effect on the remaining groups, and the locomotives are occasionally so operated when trouble develops in a motor circuit. The method, Figure 1, of tap changing involves the use of preventive coils and heavy-duty contactors, and, in order to subdivide the voltage steps, a buck-boost transformer is introduced to triple the number of steps.[4] In addition to this, a-c series motors are operated at a low magnetic flux to provide favorable commutation conditions, and in operating at start and low speed when the armature current must be high, the field is shunted, Figure 2, to keep the flux in the same range as at continuous rating. It is also necessary to provide a commutating resistor (or interpole shunt) to produce the quadrature component of the interpole flux, and this shunting must be varied in one or more steps from high power factor at low speed to a lower power factor at high speed to provide adequate neutralization of transformed voltage in the armature coils.[1] The operation of the main-field shunt and the commutating-resistor contactors is controlled by relays, Figures 3 and 4, operating from the voltage across the main field and across the armatures, respectively, Figure 5. Finally contactors must be provided to reverse the main-field connections for operation in the reverse direction.

This equipment comprises the fundamental electrical necessities of the locomotive. In addition, there are numerous electrical auxiliaries, such as forced-ventilation blowers, air compressors, heating boiler, control batteries and charging equipment, lighting and cab signal equipment. The protection of this auxiliary equipment, while necessary, will not be covered specifically in this discussion.

Protection Required

Having the picture of the main features of the locomotive, the next step is to consider against what hazards the electrical equipment must be protected. These may be stated as follows:

1. Overload
2. Overvoltage surge
3. Failure of insulation
4. Incorrect operation of tap-changing switches
5. Excessive field at start
6. Unequal motor speeds—slipping
7. Failure of cooling air
8. Transformer overheating
9. Overspeed
10. Excessive current
11. Overheating of motors

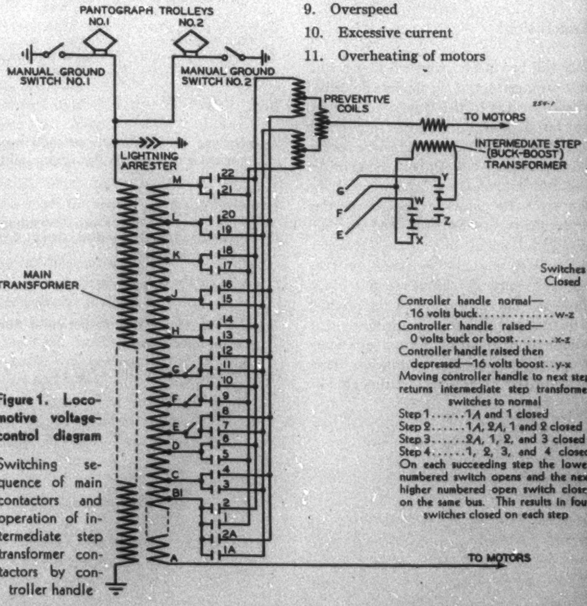

Figure 1. Locomotive voltage-control diagram

Switching sequence of main contactors and operation of intermediate step transformer contactors by controller handle

Switches Closed

Controller handle normal—16 volts buck . . . . w-z
Controller handle raised—0 volts buck or boost . . . . x-z
Controller handle raised then depressed—16 volts boost . . y-x
Moving controller handle to next step returns intermediate step transformer switches to normal
Step 1 . . . . 1A and 1 closed
Step 2 . . . . 1A, 2A, 1 and 2 closed
Step 3 . . . . 2A, 1, 2, and 3 closed
Step 4 . . . . 1, 2, 3, and 4 closed
On each succeeding step the lower numbered switch opens and the next higher numbered open switch closes on the same bus. This results in four switches closed on each step

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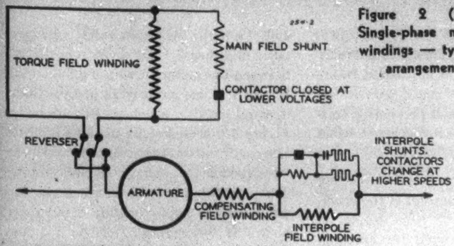

254-3
TORQUE FIELD WINDING
MAIN FIELD SHUNT
CONTACTOR CLOSED AT LOWER VOLTAGES
REVERSER
ARMATURE
COMPENSATING FIELD WINDING
INTERPOLE SHUNTS. CONTACTORS CHANGE AT HIGHER SPEEDS
INTERPOLE FIELD WINDING

Figure 2 (left). Single-phase motor windings — typical arrangement

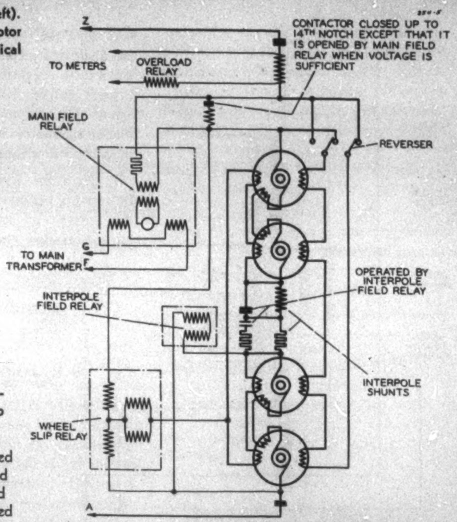

254-5
Z
TO METERS
OVERLOAD RELAY
CONTACTOR CLOSED UP TO 14TH NOTCH EXCEPT THAT IT IS OPENED BY MAIN FIELD RELAY WHEN VOLTAGE IS SUFFICIENT
MAIN FIELD RELAY
REVERSER
G
TO MAIN TRANSFORMER
F
OPERATED BY INTERPOLE FIELD RELAY
INTERPOLE FIELD RELAY
INTERPOLE SHUNTS
WHEEL SLIP RELAY
A

Figure 5 (right). Motor-field control and slip relays

▬ {Closed at low speed
Open at high speed

= {Open at low speed
Closed at high speed

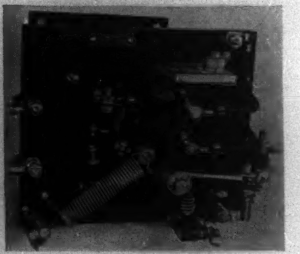


Figure 3. Interpole-field relay

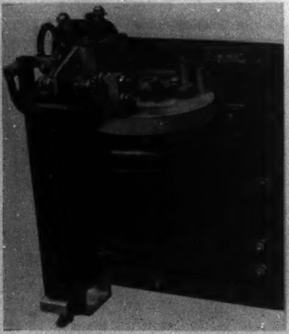


Figure 4. Main-field relay

This is a formidable list, but may be considered by classification of the manner in which protection can be provided.

Fully Automatic Protection

This method is provided for the first five conditions:

1. *Overload.* When the motor current exceeds a value beyond which it is unsafe to operate the motors for even a short period, the motor contactors are opened by the usual overcurrent relay.

2. *Overvoltage Surges.* These surges appear on the high-voltage side of the transformer and require the use of a lightning arrester.

3. *Failure of Insulation.* Faults of this nature are detected by a relay, Figure 6, known as the pantograph relay. In the secondary, or motor, circuits such detection is simplified by the fact that the circuits, Figure 7, are normally free from ground except through a current transformer, so that any secondary ground will cause the relay to operate. The primary winding of the transformer is protected by current differential between the ends of the winding. The operation of the relay first opens the motor and auxiliary switches, and, if this fails to clear the fault, an automatic switch grounds the pantograph collector, throwing the interrupting duty on to the substation circuit breaker. This relay is described in detail in the Luther paper.[2]

4. *Incorrect Operation of Tap-Changing Switches.* Tap changing on main transformer is performed by the introduction of three impedance (preventive) coils, the two smaller of which have their ends connected to four busses to which the taps from the main transformer are connected by the tap-changing contactors. The mid-points of these two preventive coils are connected to the ends of another larger preventive coil, to the mid-point of which is connected the outgoing lead to the motors. In this way four taps on the transformer are used on each of the running voltage steps, Figure 1, and a change to the next step requires the moving of only one of the four tap connections, permitting smooth and even transition in the voltage change. The impedance of the coils is sufficient to reduce the circulating current between the taps to a low value, while the reactance to the load is cancelled out by the two directional flow to the mid-point leads. In order to prevent two tap-changing contactors from closing on the same bus, thereby short-circuiting a portion of the main transformer, interlocking is used between contactors. This has been extended in recent designs to include a group of interlocking relays, Figure 8, so connected that when a contactor fails to open properly, in addition to the fact that no other

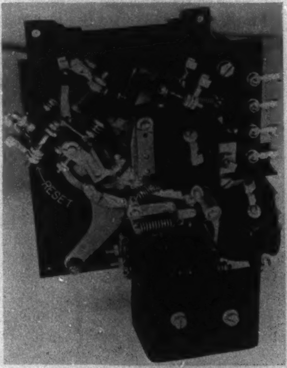


Figure 6. Pantograph relay

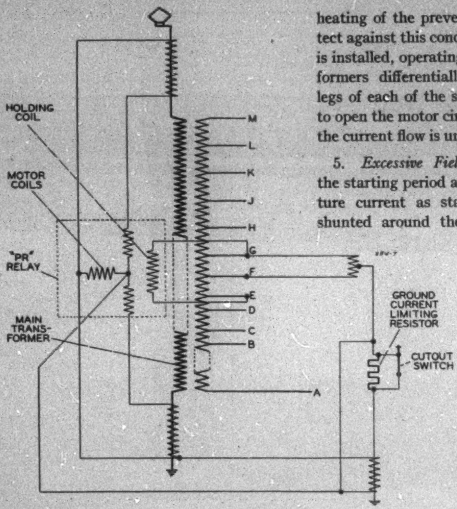
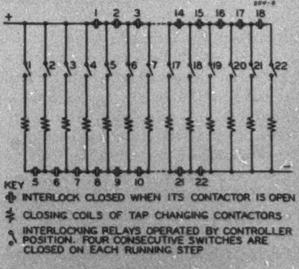

HOLDING COIL
MOTOR COILS
"PR" RELAY
MAIN TRANSFORMER
M
L
K
J
H
G
F
E
D
C
B
A
GROUND CURRENT LIMITING RESISTOR
CUTOUT SWITCH

Figure 7. Pantograph ("PR") relay connections

contactor on the same bus can be closed, the contactors on the other busses cannot proceed to the other voltage taps. This prevents a large voltage difference being established across the preventive coils if a contactor on one of the lower steps remains closed while the contactors on the other busses have closed on the higher voltage steps.

This new arrangement of interlocking requires the use of fewer interlocking fingers on the tap-changing contactors and uses instead separate interlocking relays in an enclosed cabinet, Figure 9, providing a cleaner, more accessible and reliable arrangement.

This interlocking is complete excepting under one condition, that is, when a contactor fails to close in proper sequence. Under this condition one end of a preventive coil will be disconnected from the main transformer causing an unbalance of currents which will cause excessive heating of the preventive coils. To protect against this condition a thermal relay is installed, operating from current transformers differentially connected in the legs of each of the small preventive coils to open the motor circuit contactors when the current flow is unbalanced.


+
1
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4
5
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7
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20
21
22
−
KEY
5
6
7
8
9
10
21
22
INTERLOCK CLOSED WHEN ITS CONTACTOR IS OPEN
CLOSING COILS OF TAP CHANGING CONTACTORS
INTERLOCKING RELAYS OPERATED BY CONTROLLER POSITION. FOUR CONSECUTIVE SWITCHES ARE CLOSED ON EACH RUNNING STEP

Figure 8. Simplified interlocking scheme for tap-changing contactors

5. *Excessive Field at Start.* During the starting period a portion of the armature current as stated hereinbefore, is shunted around the main, or exciting, field to minimize commutation troubles. This function is performed by a relay, Figure 4, operating from the voltage across the main field.

Semiautomatic Protection

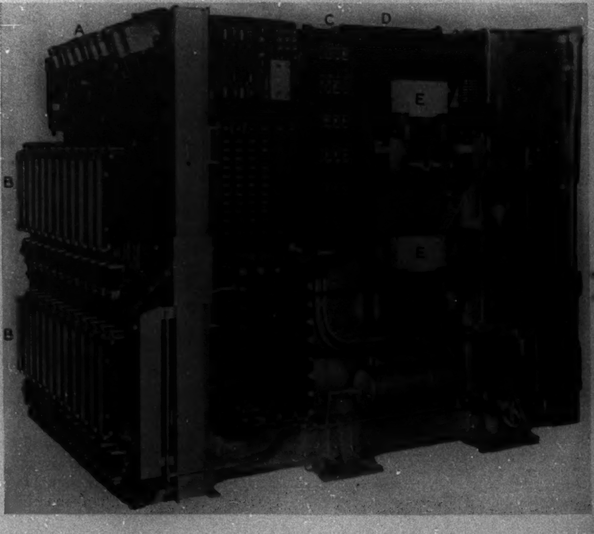
6. *Unequal Motor Speeds.* High peripheral velocity is normally designed into the traction motors, and if a pair of driving wheels slips, tractive power is lost, and the motor will soon reach a dangerously high speed. The division of voltage between the motors becomes unbalanced, Figure 4, if one motor slips or two slip at unequal speeds. A voltage balance relay, Figure 10, therefore, is used to indicate this condition to the engineman so that he may reduce the voltage on the motors. This indication is received when the slip differential is approximately 5 miles per hour. If the engineer fails to take the necessary action or is unable to control the slipping, the relay automatically opens the motor switches when the slip differential reaches 20 miles per hour.

7. *Failure of Cooling Air.* The motors and transformers are designed, because of space and economic limitations, to require forced ventilation. Failure of the air supply would permit rapid overheating. A relay in the air stream or a centrifugal relay on the blower shaft indicates such a failure to the engine crew.

8. *Transformer Overheating.* The main transformer is cooled by oil circulated by a pump. This oil passes through a radiator and is in turn cooled by air from the same blowers which provide forced ventilation to the motors. Failure of this cooling scheme to keep down the oil tem-

Figure 9. Main transformer showing voltage-control equipment

A—Auxiliary-equipment contactors
B—Tap-changing contactors
C—Interlocking relays
D—Intermediate step (buck-boost) transformer
E—Intermediate step (buck-boost) contactors



perature will operate a thermostatic alarm.

Indication

9. *Overspeed.* Speed is indicated to the engine crew by means of a speedometer. Excessive speed is dangerous to the armature windings and commutators. Dependence must be placed on the engine crews to keep the speed within limits safe to the equipment and also within the local speed restrictions of the roadway.

10. *Excessive Current.* Current is indicated by a separate ammeter for each of the parallel motor groups. The rate of acceleration is determined by the current value permitted, limited of course by the weight on driving wheels. A limit is set on the maximum motor current, which limit is appreciably below the setting of the overload relays, and proper attention to the ammeter readings during acceleration is required to prevent slipping and to stay within the range of good commutation.

Indicating Devices

Indicating lights are provided in the locomotive crew compartment to indicate the following:

(*a*). Low water in oil-fired train heating boiler

(*b*). Driving wheels slipping

(*c*). Forced-ventilation blowers stopped, or high transformer oil temperature

(*d*). Motor-overload relay operated

(*e*). Pantograph relay operated

(*f*). Main-motor-field shunting switches closed

(*g*). Fuel pump on heating boiler stopped

In addition to this, a buzzer operates to warn the crew for the first two conditions, that is, low water in heating boiler, or driving wheels slipping.

The indicating light panel, Figure 11, contains a row of amber lenses, on each of which is engraved the designation which it indicates. It is located beside and to the left of the engineman. On this same panel is the series of cab signal indication lights duplicating the aspects of the wayside signals. Here also are located the speedometer, the ammeters for each of the motor circuits, and the usual air gauges for the brake system.

Tonnage-Rating Limitation

11. *Overheating of Motors.* The temperature rise of the motor fields and armatures is the practical limit to the amount of work which the motors can do. These limitations are set by the kind of insulation used and must not exceed the permissible values, or the life of the motors will be materially shortened. Even if it were practical to have continuous indication of the controlling "hot-spot" temperatures, it would still not be practical to send out on the railroad an electric locomotive with a train which, operated under the normal speed and grade requirements of the route, might exceed the temperature limitations of the motors. In fact, no protective device, within the ordinarily accepted meaning of the term, is as yet available for avoiding excessive motor temperatures, without unduly limiting the use of the full short-time capacity of the locomotive.

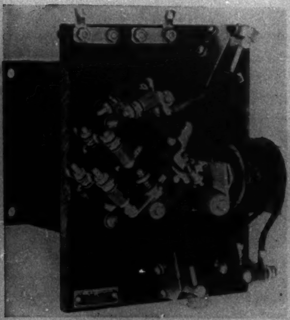


Figure 10. Wheel-slip relay

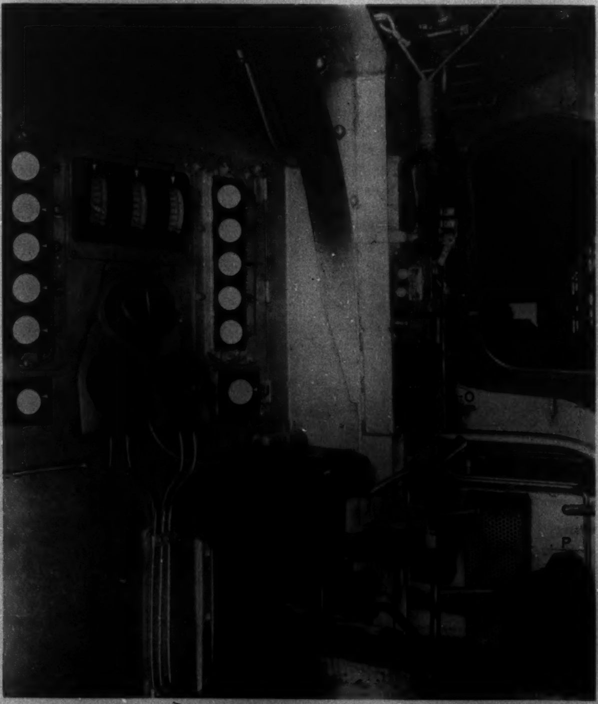


Figure 11. Indicating and operating devices in motorman's compartment

A—Ammeters
B—Low water in boiler
C—Blower-stopped and transformer-temperature indication
D—Overload relay tripped
E—Drivers slipping
F—Pantograph relay tripped
G—Fuel pump tripped
H—Speedometer
I—Air gauges
J—Field change-over
K—Cab signal indicator
L—Buzzer
M—Emergency grounding switch
N—Pantograph switch
O—Headlight switch
P—Brake valve
Q—Master controller

It is, therefore, necessary to resort to other than electrical or mechanical means of providing this very necessary protection. The obvious method is, of course, so to safeguard the operation of the locomotive as to avoid overheating of the equipment. At the same time, however, the necessity for economical operation dictates that the maximum possible work be performed by the locomotives. To meet these two conditions a tonnage rating, that is, the maximum train which can satisfactorily be handled, is determined for each class of locomotive and each route.

The working out of these ratings requires that complete motor heating and cooling characteristics be available from shop tests of the equipment. The method for arriving at these characteristics has been covered in detail in the paper by Fremont Felix and H. G. Jungk presented at last winter's convention in Philadelphia.[3]

In addition to this, the usual motor-output characteristics for different speeds and voltages are required. Then, with the profiles, alignments, and speed restrictions of the route to be used, it is possible, assuming a weight of train, to determine the motor-output requirements at all points on the runs.[5] The time, distance, and output results are tabulated, and, by using the heating and cooling characteristics of the motors, the temperature rise at each point on the route is determined. If the permissible temperature has been exceeded, the run is recalculated with a lesser weight of train. Similarly, if the maximum permissible temperature has not been reached, a new calculation is made using a heavier train. In this manner, the maximum tonnage which the locomotive can handle over each route, without overheating the motors, is determined.

In addition to these ratings, this protective method requires a thorough education of the personnel to make it effective. Yard masters must be informed of the importance of keeping the weights of trains within the specified limits. Having a proper tonnage, the engineman must learn how to operate the locomotive in a proper manner. This proper operation involves avoiding excessively difficult starts in yards and on grades, avoiding prolonged operation in weak field, taking full advantage of the momentum of the train in negotiating grades, and a general knowledge of the operating characteristics and capabilities of the locomotive.

The results of such predeterminations of tonnage ratings on the Pennsylvania Railroad have proven most satisfactory. After the ratings had been determined by calculation, they were checked by making test runs. In order to make these road tests, thermocouples were built into the windings of several of the motors. A locomotive so equipped was assigned to a regular revenue train which was built up to the predetermined tonnage for the route to be followed, and the train was operated over the road. By means of recording meters a continuous record was made of motor current and temperature. Tests of this nature have been made over all the principal routes, and in all cases the measurements have closely checked the calculations. It has not been necessary to reduce any of the calculated ratings in actual operation.

Conclusions

The electric locomotive is composed of electrical apparatus which is subject to the usual hazards of similar equipment. The methods of protection require some of the usual types of device. The specialized application, however, introduces the factors of loading and operation as protective methods to a greater extent than is usually the case with generally similar equipment in other services. It has been the purpose of this paper to summarize and describe the electrical protection of the equipment, including not only the protective devices but those features of the utilization of the locomotive which become in reality protective methods.

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Modern Electrical Equipment for Industrial Diesel-Electric Locomotives

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Synopsis: Modern electrical equipment for industrial Diesel-electric locomotives is widely different in many respects from that used five or six years ago. New and improved high-temperature insulating materials and modern synthetic varnishes have been developed and are used extensively today. Also new and improved methods of using these and the older materials have been developed. These, together with new design constants, have made possible a reduction in weight per horsepower transmitted with an improvement in the quality of the product.

The Diesel engine and generator are close-coupled and mounted as a unit, thus assuring proper alignment and, at the same time, reducing the space required for mounting on the locomotive. A new and improved method of generator excitation has been developed which assures positive control of the engine speed and power. This provides a very flexible transmission of power from the prime mover to the rail.

The modern traction motor has a cylindrical instead of a rectangular frame. It is a multipole instead of the conventional four-pole design. It is mounted integral with a double-reduction gear unit which is completely enclosed in an oiltight gear case. All bearings on the motor armature shaft and in the gear case are antifriction except those on the axle, which are sleeve bearings. All bearings in the gear unit are oil-lubricated with the same oil that lubricates the gearing. Both the high and low speed pinions are straddle-mounted to insure proper alignment of the gearing at all times.

Industrial Diesel-electric locomotives are used in slow-speed switching service where high tractive effort is most important. For this type of work, double-reduction gearing with maximum reduction shows much better transmission efficiency than the conventional single-reduction gearing.

FOR many years most of the machinery in American industrial plants has been driven electrically. During the past five or six years Diesel-electric drive has taken a definite place in moving raw materials to and finished products away from the manufacturing plants. This widespread use of electric power for locomotives has been brought about largely by two simultaneous developments.

1. *Modern High-Speed Diesel Engine.* Through standardization and the use of essentially the same Diesel engine for locomotives, trucks, tractors, and standby power plants, the cost per installed prime mover horsepower has been lowered greatly. Also, at the same time, the necessity for light weight has been realized and this, together with higher speeds, has resulted in lighter weight engines that still retain their ruggedness and ability to produce power.

Usually these engines are equipped with two-speed governors set for idling speed and full speed. Between these speeds the fuel supplied to the engine is controlled manually by the position of the locomotive operating handle. This is known as an automotive-type governor and is the type used with the equipment described in this paper.

There is another type of governor that is used sometimes in industrial Diesel-electric locomotives and is used on practically all of the larger Diesel engines. With this type engine speed is set by the governor for each position of the locomotive operating handle. In each of these positions the governor supplies the engine with the amount of fuel necessary, up to full fuel, to maintain a given speed. This is known as a variable-speed governor.

2. *Modern Electrical Propulsion Equipment.* During this same period of time, new materials and methods have been developed and adopted for use in this electrical equipment. Through design and research, improvements have been made in, and ways have been found to use, standard materials to better advantage. These have made it possible to reduce weight and cost per horsepower and at the same time improve the quality of the product.

These two developments, together with improved locomotive design, have made a very economical Diesel-electric locomotive. This industrial locomotive has low initial cost, low maintenance cost, low operating cost, and high availability. It has many of the desirable features of a straight electric locomotive but does not require a large investment for plant and equipment. Locomotives of this type weighing from 20 tons to 65 tons inclusive are being manufactured by several locomotive builders.

A unit of electrical propulsion equipment for these Diesel-electric locomotives consists of a d-c commutating pole generator connected directly to a Diesel engine, one or two d-c commutating pole motors geared to the axles, and control to assist in maintaining a smooth even flow of power from the prime mover to the wheels. There are one or more of these units per locomotive, depending upon the weight and performance required.

Traction Generators

Mechanical Features

The generators have only one bearing, either ball or spherical roller, which is located at the commutator end of the machine and carries approximately one half of the weight of the armature. This bearing is mounted in a cartridge type of housing which is so arranged that the armature can be removed without exposing the bearing to dirt and other foreign substances. The use of all-metal labyrinth seals insures that the grease will be kept in and dirt will be kept out of the bearings. The other end of the armature is bolted directly to the engine crankshaft through a flexible steel disk coupling which is mounted integral with the generator fan. This coupling is designed so that it is rigid torsionally and radially. The torsional rigidity makes it possible to use the generator armature and fan for the engine flywheel, which eliminates the conventional flywheel. The radial rigidity is necessary to maintain the electrical air gap on the generator. At the same time the coupling has flexibility in angular and axial directions to eliminate the necessity for exceptionally close machining tolerances.

Figure 1 shows a modern Diesel-electric generator.

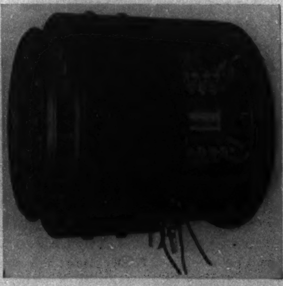

Figure 1. Modern d-c traction generator

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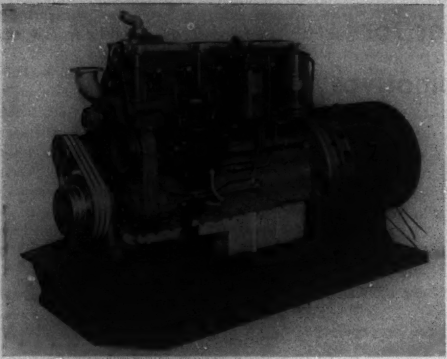

Figure 2. Modern high-speed Diesel engine with directly connected traction generator

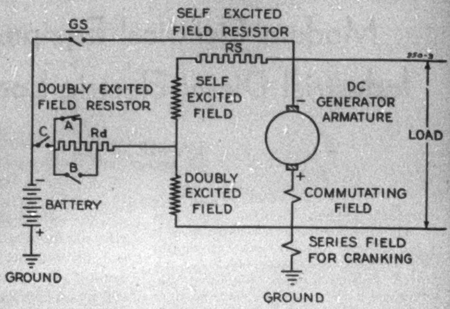

Figure 3. Diagram of connections for traction-generator excitation

The bell housing on the engine has been made special to serve the dual purpose of supporting the generator frame and providing a space in the formed openings for generator-fan discharge. When the generator frame is bolted to the bell housing and the armature to the engine crankshaft through the flexible coupling, the engine and generator become an integral unit and are mounted as such. Figure 2 shows a typical engine-generator unit. The generator fan located in the bell housing provides for multiple ventilation of the machine. One stream of air is drawn over the armature, through the air gap and between the field coils, and the other under the commutator and through the core.

Another important part of this modern generator is the improved brush holder. To collect current properly from the commutator it is necessary to have brush holders that will keep the brushes on the commutator even though it may be slightly irregular or eccentric. This is accomplished by special brush-holder, brush and brush-spring design. Care is taken to keep the brush riding against the trailing edge of the brush holder at all times. This is done by using a trailing brush with a beveled and clip top arranged so that the brush-spring pressure provides a component of force, holding the top of the brush in place, and at the same time provides the necessary radial force to keep the brush in contact with the commutator. The bottom of the brush is held in place by friction on the commutator. Due to the fact that all materials are being worked harder than ever before, brush holders must be spaced and aligned more accurately. This is accomplished by a brush holder designed so that it can be located easily and accurately, one which remains in a fixed position after it is located. These design features improve commutation, give long brush life, and insure generator characteristics that do not change from day to day due to change in brush fit or location.

The commutator must be kept tight and true. This is accomplished by using commutator-design constants which have been developed over a period of years of experience with high-speed equipments. Even with proper commutator design, difficulties occur in manufacture. These are corrected and a good commutator produced by seasoning it after the armature is completely assembled. This process consists of subjecting the commutator to centrifugal forces and thermal stresses in excess of those which are encountered in service, and tightening and grinding the commutator until it operates smoothly, both hot and cold.

In order to provide smoothness of operation, and to reduce noise and vibration to a minimum, it has been found necessary to dynamically balance the complete armature of all modern high-speed Diesel-electric locomotive generators.

ELECTRICAL FEATURES

In the past few years insulating materials and methods have been improved greatly. Some of these high-temperature materials are asbestos cloth, asbestos paper, fiber glass, and mica. With the aid of modern synthetic high-temperature varnishes, asbestos and mica, as well as fiber glass and mica, have been combined to form tapes and wrappers. These materials have made it possible to use thinner insulation of a better quality and get a better-insulated machine. This leaves more space for copper and iron and at the same time improves the heat-transfer coefficient through the insulation. Another advantage of these materials is their ability to stand high temperatures and still have long life. By using high temperatures the weight of material per horsepower transmitted is reduced. By the inherent long life of the materials, maintenance costs are kept low.

Careful study of the magnetic circuit has made it possible to so proportion the various parts of the generator that all materials are utilized to better advantage. This also has resulted in reduced weight for a given amount of power transmitted.

The modern locomotive generator is equipped with a series field winding and operates as a motor for cranking the engine from a 32-volt or 64-volt storage battery. This eliminates the starting motor drive and ring gear, and at the same time reduces the amount of apparatus to be maintained. This type of engine cranking has proved to be very successful and reliable.

The locomotive operating character-

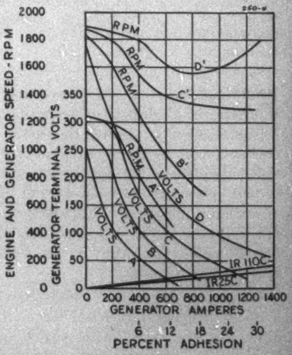

Figure 4. Characteristics of Diesel-engine traction-generator unit

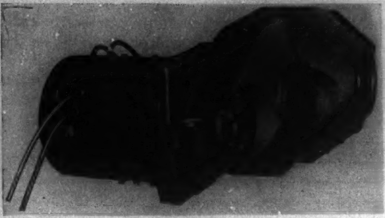

Figure 5. Modern d-c traction motor and double-reduction gear unit

istics are largely built into the generator. For a number of years Diesel-electric industrial locomotives did not have the most desirable operating characteristics. Full advantage was not taken of the fact that electric drive is the most controllable and most flexible of all drives. Recently, detailed studies have been made of various generator-excitation schemes to obtain the very best possible operating characteristics. Some of the more desirable of these are:

1. As the locomotive operating handle is moved from the idling position toward the full throttle position, the locomotive speed increases with the engine speed.

2. As the engine speeds up, the generator voltage increases to provide increased locomotive speed either with or without additional fuel.

3. Smooth acceleration is obtained without jerks or hesitation at all values of throttle opening.

4. The engine must not be loaded so heavily that it does not accelerate rapidly and evenly, nor loaded so lightly that it accelerates too fast and gives the impression of a "slipping clutch."

All of these features have been built into the newest industrial Diesel-electric locomotives by use of a generator which is essentially self-excited. This generator is provided with a doubly excited split field which receives most of its excitation from its own armature and the balance of its excitation from the storage battery on the locomotive. Each section of the field is designed so that approximately one half of the resistance of the circuit is external and does not change with temperature. This minimizes the variation of generator characteristics with temperature change in the windings.

A simple schematic diagram of the connections which are used is shown in Figure 3. The resistor R_s in the self-excited circuit is set to give the proper value of excitation for full power and full speed, Figure 4, curves *D* and *D′*, and remains set for all other values of speed and power. The resistor R_d in the doubly excited field circuit is varied in three steps by the two switches marked *A* and *B* in Figure 3. As the locomotive operating handle is moved from the idling position towards the full throttle position, R_d is set at the three different values which are necessary to insure a full even flow of power at all positions of the operating handle. These changes are made by a cam-operated switch, which is connected to the locomotive operating handle. A double cam switch, which is in the throttle-linkage mechanism between the operating handle of the locomotive and the engine fuel pump, changes the fuel pump setting to provide the proper amount of fuel.

From Figure 4 it can be seen that the speed-ampere and volt-ampere characteristics are very steep. This provides voltage and current which are supplied to the traction motors on the locomotive, causing its speed to follow that of the engine.

Referring to Figure 4, curve *A*, the first power position on the motor-current resistance (*IR*) line is at approximately 12 per cent adhesion on the locomotive. This value of adhesion has been proved by experience to be one which gives no jerks but provides a smooth, rapid start. This point will be practically the same at every start, because the volt-ampere curve crosses the motor-current resistance (*IR*) line at a steep angle, which will change very little as the temperature of the motor windings changes. This is shown by reference to the two motor-current resistance (*IR*) lines shown on Figure 4, one at 110 degrees centigrade and the other at 25 degrees centigrade. Since there are no notches on the locomotive operating handle quadrant, there are infinite steps of power and speed between the first power position, Figure 4, curves *A* and *A′*, and the full power position, Figure 4, curves *D* and *D′*. Curves *B* and *B′*, *C* and *C′* are shown to illustrate the shape of intermediate power and speed positions. Additional study of the engine-generator characteristics shows that the generator with its controlled excitation is acting to electrically govern the engine at all speeds below that at which the mechanical governor begins to operate.

Further reference to Figure 4, curves *D* and *D′*, shows that there is a wide range of amperes over which the generator volt-ampere curve is equal to the engine output, less the generator losses and a small loss of power due to the speed of the engine not being held constant. This makes it easily possible and very desirable to use only one motor combination if more than one motor per power plant is required. With a single motor, or with motors in parallel, if two motors are used per power plant, practically full power utilization can be obtained from 30 per cent adhesion on the locomotive to 60 per cent of maximum permissible operating speed. This is very desirable since it simplifies the locomotive control.

Battery-Charging Equipment

Battery charging on industrial Diesel-electric locomotives is done by one of two methods, depending on whether a 32-volt or 64-volt battery is used.

The 32-volt battery is charged with automotive-type equipment. This consists of a 750-watt shunt-wound generator which is mounted on the engine in such a way that it can be driven by a belt or from an auxiliary shaft. A vibrating contact regulator is used in the field of the generator to hold approximately constant voltage over a range of charging current, which is limited by a series coil in the regulator. The generator is self-ventilated by a small fan.

When a 64-volt battery is used, a 1.5-kilowatt shunt-wound charging generator is used. It is mounted on the floor of the locomotive and belt-driven from a pulley on the end of the traction generator shaft. The voltage on the charging generator is held constant by a vibrating contact

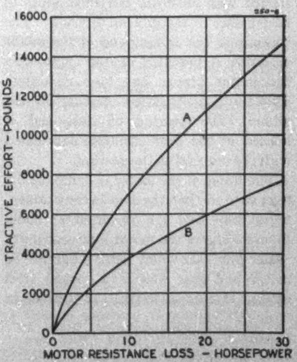

Figure 6. Comparative motor-resistance-loss curves for traction motors with single-reduction and double-reduction gearing

regulator operating in the shunt field. The current is limited by a ballast resistor in the battery-charging circuit. This generator is self-ventilated by a fan mounted directly on the armature shaft.

Traction Motor and Gearing

MECHANICAL FEATURES

The modern Diesel-electric industrial locomotive traction motor is very different from the conventional railway motor. The new motor has a rolled-steel cylindrical fabricated frame instead of a cast-steel rectangular frame. This new construction has a number of manufacturing advantages and also is more desirable magnetically since casting blowholes are eliminated. The motor is very accessible for original assembly and for repairs. It is equipped with double-reduction gearing instead of the conventional single-reduction gearing.

The commutator is of special design to meet service requirements. In addition to the use of design constants that give a sturdy strong commutator, it is given a thorough seasoning after the armature has been completely assembled.

The armature is designed to withstand the stresses of high-speed operation that go with double-reduction gearing. The coils are held in the core slots by wedges and the end windings are held with binding wire. The completed armature is dynamically balanced to very close limits so that it operates smoothly and with very little vibration at all speeds. It is also equipped with antifriction bearings on both ends. The commutator end bearing housing is designed so that the armature can be removed without opening the bearing housing and exposing the bearing to dirt. This bearing is also arranged with all-metal labyrinth seals to keep grease in and to keep dirt out. The bearing on the opinion end of the motor armature is mounted on the shaft after the motor pinion has been mounted. This forms a straddle-mounting for the pinion. The bearing on this end is housed in the gear case and lubricated with the same oil as the gearing.

The frame of the motor is bolted to the gear case so that the two become an integral unit and are mounted as such. Figure 5 shows the motor and gear unit.

The gear case which forms a part of the motor and gear unit is a rugged steel casting of special design. It is made in one piece except the gear cover which is a malleable iron casting and is bolted to the gear case along a 45-degree line. All bearings and pinions are assembled so that they can be replaced without removing the motor and gear unit from the locomotive. The motor and gear-unit support is so located that, for either direction of motion of the locomotive, there will be no tilting to throw the gear mesh out of alignment or place a twisting load on the axle bearings under the heavy tractive efforts encountered in service. This type of hardened gearing operating in an oiltight gear case has a long life, which should be equal to that of the locomotive. The gear unit is equipped with flood-lubricated sleeve axle bearings and thrust surfaces, and with antifriction intermediate and high-speed bearings operating in an oil bath. Both the high-speed and low-speed pinions are straddle-mounted to maintain alignment. The gearing is straight spur on both reductions.

The motor is self-ventilated. This is found to be practical in a motor which is used with double-reduction gearing, since even in slow-speed switching service, motor speeds are high enough to make the motor fan effective. The fan which is mounted on the armature shaft at the end opposite the commutator provides for multiple ventilation of the machine.

ELECTRICAL FEATURES

In the motor use has been made of the various insulating materials, varnishes, and methods used in the traction generator. By this means the best possible quality has been obtained with the smallest amount of insulation. This has made it possible to get more tractive effort rating per pound of material used.

In order to further conserve material, motors have been designed with more than the conventional four poles. The latest industrial Diesel-electric locomotive motor is a six-pole machine. The multipole design reduces length of end windings and commutator. Also the use of multipoles provides for better distribution of field windings. This, in connection with detailed studies of the magnetic circuit, has enabled the designer of modern motors to materially reduce the size and weight of the field structure.

The field coils have also been reduced in size by the use of new insulating materials and methods. Both exciting and commutating coils are edgewise-wound and formed to fit the poles. The completed coils after being mounted on the poles receive a number of dips and bakes in modern synthetic insulating varnish. With this type of coil the heat-transfer coefficients are much higher than for a conventional coil. After the poles and coils are assembled in the frame, the complete assembly is dipped in modern synthetic varnish and baked to fill completely and insulate all connections and joints so that they are sealed and protected from water, oil, and other foreign materials that may enter the motor.

The design constants of the motor have been made such that the motor has a very steep speed curve, and a wide range of field control can be used. These, together with the engine-generator characteristics, Figure 4, make a locomotive with a wide range of full utilization of available engine horsepower.

Since industrial Diesel-electric locomotives are essentially slow-speed machines, and transmission efficiency is an important factor, double-reduction gearing is superior to single-reduction gearing for this service. A much greater reduction can be obtained with double-reduction gearing *than with single-reduction gearing*, and, *at the same time*, clearance under the gear case *can be* maintained, as specified by the Interstate Commerce Commission. This can be done while still having ample top speed for industrial service. Figure 6 shows motor losses at 110 degrees centigrade copper temperature, with an assumed two volts for brush drop, under starting conditions where good efficiency is most difficult to obtain, and where the difference between transmissions is most noticeable. Figure 6, curve *A*, is for the double-reduction motor with maximum reduction.

Figure 6, curve *B*, is for a comparable single-reduction conventional motor with maximum reduction. A study of this curve clearly shows the advantage of double reduction for locomotives that are not required to operate at high speed but are required to provide high tractive effort. Since at the starting point of the locomotive all the losses are in resistance, the difference between curve *A* and curve *B* in Figure 6 shows the additional tractive effort that can be produced with the double-reduction gearing over the single-reduction gearing for a given horsepower loss.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1942 Supplement to Electrical Engineering—Transactions Section

Correction for Saturation

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IT is generally agreed that the virtual or air-gap voltage of a salient-pole synchronous machine determines the saturation in the magnetic circuit. Studies have indicated that the major portion of the saturation occurs in the poles of such machines. Consequently, it is logical to apply a correction as a linear addition to the nominal or excitation voltage vector of the two-reaction diagram drawn with unsaturated constants, in order to ascertain the actual per-unit excitation necessary under load conditions which produce a certain air-gap voltage. It is the purpose of this paper to present an analytical method for determining the necessary correction.

Careful tests have shown that the correction for saturation which is to be added to the excitation voltage obtained without saturation can be quite accurately determined for a given condition of loading by moving the zero-power-factor saturation curve for that load so that it coincides with the no-load saturation curve below saturation.[1] In Figure 1 the dashed curve is determined by moving the zero-power-factor load-saturation curve to the left a distance $Od = bc$. The horizontal intercept ab between the air-gap line and the dashed curve is the total per-unit correction corresponding to the condition of loading which produces a per-unit value of i_d equal to the per-unit armature current of the load-saturation curve, and a component of air-gap voltage in phase with the excitation voltage equal to Oe.[1,2]

If a family of zero-power-factor load-saturation curves is available, by moving each curve to coincidence with the no-load saturation curve as explained above, a family of dashed correction curves is obtained. Since at zero power factor $i_a = i_d$, it follows that *the parameter for this family of correction curves is* i_d. Having such a family of curves the procedure would be to determine the excitation voltage by drawing an unsaturated two-reaction diagram in accordance with the equivalent air-gap line drawn through a point on the no-load saturation curve equal to the air-gap voltage for the desired condition of loading. From this diagram i_d could be determined, and from the family of correction curves the intercept between the air-gap line and the curve of parameter i_d could be read corresponding to the direct-axis component of the air-gap voltage.

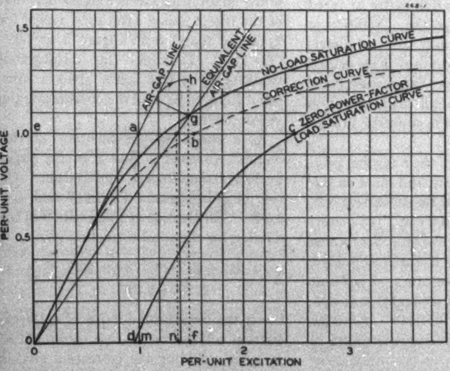

Figure 1. Saturation curves

The foregoing method of correcting for saturation is predicated upon the possibility of obtaining a family of correction curves, and this, in turn, depends on the ease and accuracy of the method used to secure the load-saturation curves. If the machine is already built, it may be possible to get these curves by test methods, but unless these methods involve but a few simple no-load tests the practical importance of the curves is nil. The greatest value of the correction curves would come in designing synchronous machines and in the predetermination of their performance. An empirical procedure which facilitates the application of the above principles is to express the saturation curves by a general mathematical equation and to employ saturation factors derived from the no-load saturation curve.

A modification of Froelich's equation for expressing a family of saturation curves is as follows:

$$V = \frac{A(i - Ci_d)}{B + i - Di_d} \tag{1}$$

wherein

A, B, C, and D are constants to be evaluated
i = per-unit field amperes or ampere turns
i_d = per-unit direct-axis component of armature current
V = per-unit volts

Solving the equation for field excitation,

$$i = \frac{BV + (AC - DV)i_d}{A - V} \tag{2}$$

From equation 1 it is seen that when $i = Ci_d$, $V = 0$. Hence, by subtracting Ci_d

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from the right side of equation 2 the expression for the correction curve is obtained. As a result, for the correction curve

$$i=\frac{BV+(C-D)Vi_d}{A-V} \qquad (3)$$

The equation for the air-gap line, in per-unit values, is

$$i=V \qquad (4)$$

The *total correction for saturation* is the difference between the air-gap line and the correction curve. Whence, subtracting equation 4 from equation 3

$$\Delta=\frac{V[V+(B-A)+(C-D)i_d]}{A-V} \qquad (5)$$

To evaluate the constants of the preceding equations it is necessary to have the no-load saturation curve, the unsaturated per-unit direct-axis synchronous reactance, and one point on the full-load zero-power-factor saturation curve at about rated voltage. Constants A and B are found by solving two simultaneous equations which are derived by choosing two corresponding sets of values of V and i from the no-load saturation curve and substituting them into the relation

$$V=\frac{Ai}{B+i} \qquad (6)$$

which is the expression for the no-load saturation curve, obtained from equation 1 by making i_d equal zero. The values of V and i should be chosen with one set at rated voltage and the other set with considerable saturation.

The constant C is numerically equal to the unsaturated per-unit value of direct-axis synchronous reactance. From equation 1 it is seen that when $V=0$, $C=i/i_d=i$ when $i_d=1$ per-unit. Excitation i is necessary to circulate rated current when the machine is short-circuited. If i is held constant and the short circuit is removed, the voltage of an unsaturated machine will rise to the air-gap line. This voltage is the unsaturated synchronous-impedance drop. By virtue of the definition of unit field excitation as that value which produces unit volts at the air-gap line, the per-unit value of i must equal the per-unit value of unsaturated direct-axis impedance. Hence, neglecting armature resistance, C is numerically equal to the unsaturated per-unit x_d.

If a test point on the full-load zero-power-factor saturation curve near rated voltage is available, the constant D may be found by substituting the corresponding values of V and i for this point into equation 1, along with the values of A, B, and C, letting i_d equal unity.

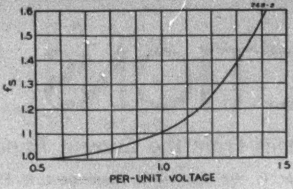

Figure 2. Saturation factor

When no test point on the full-load zero-power-factor saturation curve is available, the following empirical procedure may be used. At zero power factor the per-unit air-gap voltage may be taken equal to the algebraic sum of the per-unit terminal voltage and the per-unit armature-leakage reactance. Through a value of voltage on the no-load saturation curve equal to the per-unit air-gap volts, draw an equivalent air-gap line. In Figure 1 point g represents a per-unit value of air-gap volts. At rated voltage unit field excitation for the equivalent air-gap line is greater than unit excitation for the actual air-gap line by the ratio On/Om. Therefore, the excitation-voltage vector from an unsaturated two-reaction diagram referred to the actual air-gap line must be multiplied by this ratio to refer it to the equivalent air-gap line.

To account for increased saturation due to the presence of armature current a saturation factor[3] may be used. In Figure 1 the saturation factor corresponding to g is the ratio fh/fg; gh is the perpendicular distance of point g from the air-gap line. A corresponding factor for other voltages on the no-load saturation curve may be found in a similar manner. A plot of saturation factors as a function of per-unit volts is shown in Figure 2.

A point on the correction curve corresponding to rated voltage is found as the product of the saturation factor at rated volts and the ratio On/Om. Addition of the unsaturated per-unit value of direct-axis synchronous reactance drop caused by load current i_d gives the desired point on the zero-power-factor load-saturation curve for substitution in equation 1 to determine constant D.

As an illustration of the foregoing principles consider the computation of excitation characteristics for the 40-horsepower, 440-volt, 6-pole, 1,200-rpm, 0.8-power-factor synchronous machine for which test data are given by Robertson, Rogers, and Dalziel.[1] The unsaturated per-unit values of the machine constants are: $r_a=0.04$, $x_a=0.086$, $x_d=1.045$, $x_q=0.55$. The no-load saturation curve is shown in Figure 1.

To determine constants A and B values of $V=1.0$ and $V=1.4$ are chosen. Respective values of i are 1.25 and 3.04. Substitution of these values in equation 6 gives two equations from which it may be determined that $A=1.906$ and $B=1.133$.

C is found from the test data as being equal numerically to $x_d=1.045$.

From Figure 1 the ratio $On/Om=1.38$.

From Figure 2 the saturation factor corresponding to rated voltage is seen to be 1.1.

Applying the procedure outlined above the per-unit excitation for rated voltage at the full-load zero-power-factor saturation curve is $(1.1\times1.38)+1.045=2.563$. Substitution of these values in equation 1 gives

$$1.0=\frac{1.906(2.563-1.045)}{1.133+2.563-D}, \qquad \text{whence } D=0.803$$

By equation 5 the correction for saturation is

$$\Delta=\frac{V(V-0.773+0.242i_d)}{1.906-V}$$

In applying this relation i_d is scaled from the two-reaction diagram and V is interpreted as the projection of the air-gap voltage on the excitation voltage. For leading power factors with generators and for lagging power factors with motors the effect of armature reaction is magnetizing. In these cases the sign of i_d must be taken negative.

When drawing the two-reaction diagram both the per-unit excitation and the power angle are usually desired. It has been shown that it makes little difference in the excitation whether the saturated or unsaturated constant x_q be used, but the power angle is definitely influenced.[1] The saturated value of x_q should be used for accuracy. Since the rate of saturation of the quadrature axis is roughly half that of the direct axis a saturation factor for x_q can be found from Figure 2 as $1.0+(f_s-1)/2$. Dividing the unsaturated value of x_q by this factor gives a saturated value to be used in constructing the vector diagram.

As a check on the accuracy of this method, consider Figure 5 of the paper by Robertson, Rogers, and Dalziel.[1] On this vector diagram $V=1.217$ and $i_d=0.647$. Substitution of these values in the relation for the total correction gives

$$\Delta=\frac{1.217(1.217-0.773+0.157)}{1.906-1.217}=1.06$$

Using the unsaturated value of x_d the excitation voltage for the case shown is 1.85 per unit. Adding the correction gives a

A 2,500,000-Kva Compressed-Air Powerhouse Breaker

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I. Introduction

THE use of compressed-air breakers for indoor service has become established by several field installations since the presentation of papers[1,2] describing these devices two years ago. At that time complete tests had been made justifying ratings to 1,500,000 kva. Since then, powerhouse requirements have demanded the development of similar breakers for 2,500,000 kva. During the same interval new laboratory facilities have been provided[3] which are capable of completely testing these large breakers. This paper describes the theory and construction of this new breaker and for the first time presents test results of full 2,500,000 kva under three-phase fault conditions, together with a study of associated voltage recovery rates.

With the completion of the 2,500,000-kva rating, a complete series of indoor air breakers from 500,000 kva up is available which are common in fundamental design and operating pressure. In fact, compressed air was chosen as the interrupting medium because it was found after study of many types of interrupter that this means alone was adequate throughout the range for an oilless breaker.

II. Transverse-Blast Compressed-Air Interrupters

The transverse-air-flow type of interrupter has been shown to be capable of interrupting currents in excess of 60,000 amperes and fundamentally does not appear to have the current limitation inherent in a nozzle type of interrupting element. Consequently, for the 2,500,000-kva breaker this general type of interrupter was chosen. Figure 1A shows a diagrammatic sketch of the form of the interrupting device, and Figure 1B shows a partial assembly of the actual apparatus.

The interrupter is built up of alternate splitter plates and cooler units, disposed substantially parallel to the air blast and perpendicular to the arc. The arc is drawn from back to front across the discharge end of the blast tube and blown into the ends of the slots of the splitter plates. The moving contacts are of the blade type, hinged at the lower end and rotated back and forth by an insulated pull rod. The outer contacts are for carrying current only, and the middle one has the additional function of drawing the arc in the interrupter. The current-carrying contacts are completely isolated from the arcing contact, and, upon opening, they part approximately one inch ahead of arcing contacts. In the closed position, deep engagement into the stationary finger contacts is obtained. In the full open position the moving contacts are all completely withdrawn from the interrupter, so that an isolating air gap is placed in series with the interrupter, and all contacts are accessible for inspection. The assembly as shown is easily removed from the breaker; likewise, the interrupter can be removed as a unit from the contact assembly and supporting structure.

The actual interruption in an arc chamber of this type is accomplished by the air stream driving the arc against and between the lower ends of the splitter plates. When a normal current zero is reached the arc core is in a comparatively highly ionized condition, as indicated by the considerable leakage current and damping effect of the breaker on the recovery voltage, after the current zero.

The rate of deionization in a conducting gas column depends largely on conditions at the boundary where diffusion and recombination are most effective; consequently, it is very important to approach the current zero with a highly turbulent atmosphere surrounding the arc core. In compressed-air circuit breakers these boundary conditions are largely determined by the way in which the air flows with respect to the arc. If an arc is blown into a splitter of refractory material, it will be forced against the edge of the splitter, where it will remain more or less stationary with respect to the splitter. The flow of high-velocity air parallel to the splitter will cause one side of the arc to be highly turbulent, a condition very favorable to deionization. The other side of the arc, however, will be closely pressed to the splitter where the air velocity is substantially zero, and deionization will proceed very slowly. If, however, the splitter is made of some gas-evolving material such as fibre, an entirely different set of conditions will exist adjacent to the splitter.[4]

As the air flow, parallel to the splitter, forces the arc against the splitter edge, the heat from the arc liberates gas from the fibre which projects itself away from the splitter and into the arc stream at high

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per-unit excitation of 2.91 which, by Figure 4 of the paper is equivalent to $2.91 \times 3.6 = 10.5$ amperes. Table I of the paper shows a test value of 10.7 amperes.

Consideration of Figure 6 in the paper by Robertson, Rogers, and Dalziel[1] shows for an underexcited motor a condition of loading where $V = 1.095$ and $i_d = 0.25$. Here

$$\Delta = \frac{1.095(1.095-0.773-0.06)}{1.906-1.095} = 0.352$$

The excitation voltage from this diagram is found to be 0.835. Whence the total excitation is $0.835 + 0.352 = 1.187$. This is equivalent to $1.187 \times 3.6 = 4.27$ amperes. Table I shows a test value of 4.36 amperes.

Application of the above procedure to other machines has shown that this empirical method will result in values of per-unit excitation under saturated conditions which check fairly closely with actual test results. Besides giving reasonable accuracy it has the further advantages of requiring a minimum of test (or predetermined) data and of being easy to apply.

References

1. The Saturated Synchronous Machine, B. L. Robertson, T. A. Rogers, and C. F. Dalziel. AIEE Transactions, volume 56, 1937, July section, pages 858-63.

2. Synchronous Machines—I and II, R. E. Doherty and C. A. Nickle. AIEE Transactions, volume 45, 1926, pages 912-26; 927-42.

3. Equivalent Reactance of Synchronous Machines, S. B. Crary, L. A. March, and L. P. Shildneck, AIEE Transactions, volume 53, 1934, January section, pages 124-32.

Table I. Three-Phase Opening and Closing–Opening Tests on a Westinghouse Compressed-Air Circuit Breaker

4,000 Amperes, 15 Kv; 2,500,000 Kva Tested 13.2 Kv

Test No.	Current Interrupted (RMS Amperes) Phase 1	Phase 2	Phase 3	Inches Contact Separation at Arc Extinction Phase 1	Phase 2	Phase 3	Arcing Time (Cycles) Phase 1	Phase 2	Phase 3	Tank Pressure (Lb Per Sq In.)	Operating Duty	Circuit Transient Recovery-Voltage Rate (Volts Per Microsecond)
1	7,200	6,200	7,400	1.3	0.8	1.3	0.6	0.5	0.6	150	O	700
2	6,800	7,200	6,200	1.2	0.8	1.2	0.6	0.5	0.6	150	O	700
3	6,800	7,100	6,200	1.3	0.9	1.3	0.7	0.6	0.7	150	CO	700
4	6,200	7,900	7,700	1.2	1.4	1.4	0.6	0.7	0.7	150	O	700
5	24,700	20,000	27,000	2.0	1.4	2.0	0.7	0.6	0.7	150	O	1,170
6	31,000	22,000	20,000	1.4	1.1	1.4	0.7	0.6	0.7	150	CO	1,170
7	*48,000	*49,000	*49,000	1.4	1.4	0.8	0.7	0.7	0.5	150	CO	1,750
8	66,000	53,000	62,000	2.0	2.0	1.2	0.6	0.6	0.3	150	O	1,750
9	84,000	70,000	82,000	3.8	3.8	2.2	0.7	0.7	0.5	150	O	2 080
10	91,000	73,000	90,000	1.4	2.7	2.7	0.4	0.5	0.5	150	O	2,080

* Currents closed (amperes)

	Phase 1	Phase 2	Phase 3
Crest values	150,000	95,000	148,000
Rms values	92,000	59,000	86,000

velocity. This gas bombardment causes the arc stream to be highly turbulent on the side toward the splitter, and the air flow causes the arc to be highly turbulent on the side away from the splitter. Thus, the entire body of the arc lends itself very readily to deionization by diffusion, by reason of its high turbulence.[4] As a result, the space adjacent to a single splitter can be made to recover dielectric strength at a rate equal to the rate of rise of recovery voltage of the fastest powerhouse circuits, providing the ionized gas preceding the current zero is removed as fast as the current decreases toward its zero value. This condition is easily obtained if the rms value of the current being interrupted is small. As the current increases, the amount of gas liberated from the splitters becomes greater, and the air blast may become incapable of removing it fast enough. A 60,000-ampere arc in an arc-chute throat two inches wide will liberate sufficient gas from the splitter to cause the flow of gas to reverse a distance of six inches against a driving pressure of 150 pounds per square inch. If the rate of rise of recovery voltage of the circuit is slow, the stalled flow will have time to recover velocity, and dielectric will be restored. However, if the recovery rate is fast, the space below the splitters will remain clogged too long and reignition will result. Enlarging the throat of the arc chute or increasing the air pressure will obviously improve conditions; however, both result in an increased air consumption for a nominal gain in interrupting ability. Decreasing the volts per splitter by increasing the number of splitters results in some gain, but again as the number of splitters is increased, the gas-evolving surface is also increased, and the gas removal becomes more of a problem.

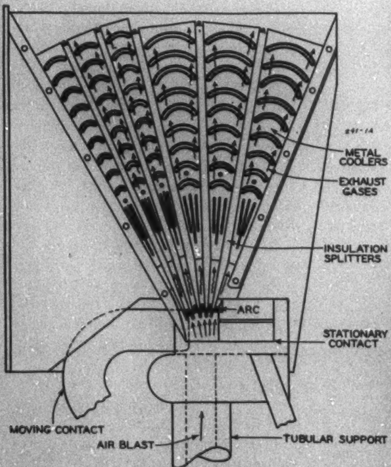

Figure 1A (left). Schematic diagram of a transverse-flow compressed-air circuit breaker

Figure 1B (right). Pole-unit assembly of a 2,500,000-kva 15-kv compressed-air circuit breaker showing the contacts and partially dismantled interrupter, after having interrupted 11 short circuits in excess of 1,500,000 kva

Likewise, spacing the splitters further apart allows more gas to escape, but this allows the arc to loop between the splitters a greater distance which in turn causes more gas to be liberated.

Figure 2 shows the fundamentals of a simple solution to this problem. The arc chute consists of a narrow throat portion and a wide portion. The splitters are full width and terminate at the narrow throat, having a notch cut into them just above the throat. The arc is blown into this notch which is well up into the wide portion of the chute. Since the throat is narrow and the approach to it is streamlined, the air approaches the arc with a high velocity. When the instantaneous value of the current is large, the arc does not block the flow of air, but exerts only enough pressure on it to deflect it to the sides, thus allowing the flow to continue. This diverted flow washes away the extraneous ionized flame as the instantaneous value of current decreases. By providing an escape for a continuous flow of air, the pressure on the arc never exceeds that which is necessary to deflect the air around it, and consequently, it is not pressed too tightly against the splitters which always results in too great evolution of gas and greater clogging. By using the means shown in Figure 2, it has been possible to build the interrupter for 2,500,000 kva in reasonable dimensions and obtain entirely satisfactory operation with a tank pressure of 150 pounds per square inch, even with very high-voltage recovery rates.

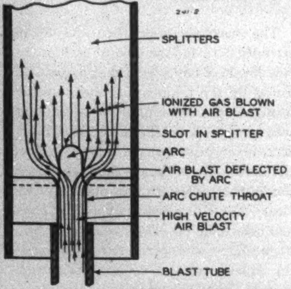
241-2
SPLITTERS
IONIZED GAS BLOWN WITH AIR BLAST
SLOT IN SPLITTER
ARC
AIR BLAST DEFLECTED BY ARC
ARC CHUTE THROAT
HIGH VELOCITY AIR BLAST
BLAST TUBE

Figure 2. Cross-sectional diagram of arc chutes showing air flow under arcing conditions

III. Breaker Construction and Operation

Figure 3A shows a 4,000-ampere breaker mounted in a steel cell and set up for test at the East Pittsburgh high-power laboratory, for the Consolidated Edison Company. Figure 3B shows a schematic side elevation of the same breaker. It consists essentially of an air-storage tank at the bottom, an air-operating mechanism at one side, suitable connecting levers for operating the contacts, an air-duct between the tank and the vertically situated arc chamber, and a separate mechanically operated blast valve for each pole.

This type of construction naturally places all parts of the breaker which operate at ground potential including the pneumatic and electric control, in the lower compartment of the cubicle. The tank and mechanism which constitute the heaviest parts of the breaker may, therefore, be supported directly on the floor where it is most accessible and completely separated by a horizontal barrier from the high potential compartment just above. This compartment, shown with the doors standing open, houses the contacts, interrupting elements, and all other live parts. The phases are separated from each other by removable vertical barriers extending inwardly from the hinge points of the doors. The contacts are very accessible for inspection; likewise, the barriers and interrupters may be easily withdrawn. Above the interrupter are diffusion chambers and mufflers, one for each phase. Upon interruption, the three mufflers discharge their gas into one common gas receiving chamber, which communicates with the outside through ventilating grilles as shown.

The flow of air for arc interruption is, therefore, as short and direct as possible, with no turns as it passes from the tank, through the blast valves and blast tubes across the arc and through the interrupter into the muffler and gas-receiving chamber at the top.

This is a very important factor in reducing pressure requirements and the quantity of air consumed. Tests have shown that with earlier experimental forms of breakers built with a single blast valve and numerous turns in the blast air-supply piping that the major portion of the energy stored in the air during compression is dissipated before the air stream

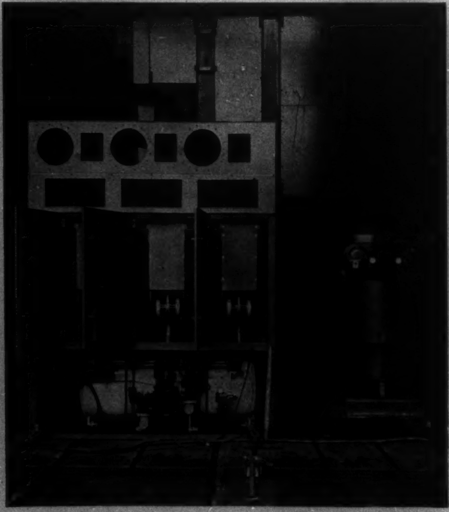

Figure 3A (left). Three-pole 2,500,000-kva 15-kv 4,000-ampere compressed-air circuit breaker in steel cell, set up for test at East Pittsburgh high-power laboratory

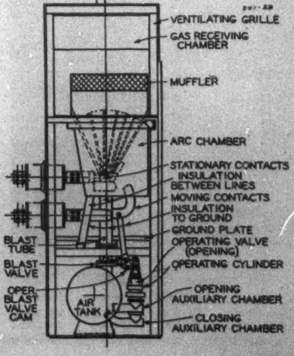
241-3B
VENTILATING GRILLE
GAS RECEIVING CHAMBER
MUFFLER
ARC CHAMBER
STATIONARY CONTACTS
INSULATION BETWEEN LINES
MOVING CONTACTS
INSULATION TO GROUND
GROUND PLATE
OPERATING VALVE (OPENING)
OPERATING CYLINDER
OPENING AUXILIARY CHAMBER
CLOSING AUXILIARY CHAMBER
BLAST TUBE
BLAST VALVE
OPER. BLAST VALVE CAM
AIR TANK

Figure 3B (right). Sectional elevation showing side elevation of the breaker in Figure 3A

reaches the arc chamber. With straight-line air flow these losses are reduced several fold. The three mechanically operated blast valves can be closely timed with respect to contact separation so that an extremely high rate of flow of air through the interrupter occurs during actual interruption, but the over-all air consumption is quite nominal.

The operating mechanism consists of a single air cylinder, in which a piston moves vertically. The piston is connected to a torsion shaft through a lever, piston rod, and a single set of links. The torsion shaft operates three insulating pull rods which in turn are attached to the moving contacts of the three pole units. The blast valves (one for each pole unit) are located at the top of the tank and are operated by rocker arms from cam surfaces which are a part of the torsion shaft. It is, therefore, possible to accurately synchronize the opening of the blast valve with the drawing of the arc in the interrupter. The cams and their co-operating parts are arranged to open the blast valve during the opening motion of the contacts only.

To open the breaker, air is admitted to the upper side of the piston from the opening auxiliary chamber, through a pneumatic relay, in response to a tripping impulse in an electropneumatic pilot valve. To close the breaker, air is admitted below the piston, from the closing auxiliary chamber, through a pneumatic relay in response to a closing impulse in another electropneumatic pilot valve.

The volume of each of the auxiliary chambers is adjusted to some suitable percentage of the volume of the operating cylinder and communicates with the main-pressure storage tank through a small hole of such size that it can charge an auxiliary chamber to full pressure in approximately one second. However, only a small amount of air can flow through this hole during the fraction of a second that it takes the piston to move from one end of the cylinder to the other. Thus, for instance, if the volume of the opening auxiliary chamber is one half of the volume of the operating cylinder, the pressure on the piston at the end of its travel will be approximately one third of the pressure at the beginning of its motion. By this means, it is possible to obtain an accelerating force of several thousand pounds at the beginning of motion and reduce this force to any predetermined value toward the end. This simple force system together with the toggle makes it possible to close the breaker against the heaviest short-circuit current; lock it in the closed position by moving the toggle over center, and upon opening, obtain high-speed contact separation.

If, however, it is necessary to open the breaker immediately after closing, the air below the piston which was used to close the breaker must be exhausted. To take care of this, an exhaust valve is built into the side of the cylinder and actuated by the motion of the piston. This insures that an opening operation is independent of the time the breaker has remained in the closed position; likewise, it can be used to insure that the closing is independent of the time the breaker has remained in the open position. The equivalent of trip-free opening and high-speed reclosing is thus accomplished without latches, springs, or trip-free levers.

IV. Air-Supply System

The mechanical operation and arc interruption of indoor compressed-air circuit breakers has been found to be unaffected by the temperature and moisture content of the air supply. However, moisture removal from the air is desirable to decrease corrosion, protect insulation, and eliminate any tendency from ice formation in the valves or other pneumatic devices. These provisions are met by a system consisting of an intake filter, a two-stage compressor operating to 250 pounds per square inch; a cooling coil, small-storage, and moisture-elimination reservoir; a second cooling coil and large main-storage reservoir. This system is connected to the line through an automatic reducing valve which drops the pressure to 150 pounds per square inch.

Elimination of oil vapors in the air system, essential to prevent explosion, is provided by the use of large slow-running compressors to prevent undue temperature rise and adequate cooling by suitable coils to condense oil vapors. The compressor is also of such a design that it is

Table II. Three-Phase Opening and Closing–Opening Tests on a Westinghouse Compressed-Air Circuit Breaker

4,000 Amperes, 15 Kv, 2,500,000 Kva Tested 13.2 Kv Line to Line, 60 Cycles

Test No.	Current Interrupted (RMS Amperes)			Inches Contact Separation at Arc Extinction			Arcing Time (Cycles)			Tank Pressure (Lb Per Sq In.)	Operating Duty	Circuit Transient Recovery-Voltage Rate (Volts Per Microsecond)
	Phase 1	Phase 2	Phase 3	Phase 1	Phase 2	Phase 3	Phase 1	Phase 2	Phase 3			
11	7,300	6,200	8,300	0.9	1.1	1.1	0.3	0.4	0.4	150	O	700
12	6,100	7,750	7,300	1.3	1.1	1.3	0.5	0.4	0.5	150	O	700
13	7,100	6,000	7,900	2.1	1.2	2.1	0.7	0.4	0.7	150	O	700
14	7,400	6,000	6,900	1.5	1.5	1.0	0.6	0.6	0.4	150	O	700
15	8,200	6,800	6,400	1.0	1.5	1.5	0.4	0.6	0.6	150	O	700
16	70,000	84,000	84,000	3.0	3.0	1.0	0.5	0.5	0.2	150	O	2,080
17	8,300	7,700	6,100	1.1	2.1	2.1	0.4	0.6	0.6	150	O	700
18	7,700	6,900	6,400	1.1	2.0	2.0	0.4	0.5	0.5	150	O	700
19	98,000	80,000	82,000	2.0	3.5	3.5	0.5	0.6	0.6	150	O	2,080
20	7,800	8,300	6,300	1.5	1.5	1.0	0.5	0.5	0.4	150	O	700
21	102,000	98,000	80,000	3.0	3.8	3.8	0.4	0.6	0.6	150	O	2,080
22	105,000	93,000	82,000	3.8	2.5	3.8	0.8	0.6	0.8	125	O	2,080
23	48,000	51,000	49,000	2.7	2.7	1.7	0.7	0.7	0.5	125	CO	1,750
24	6,700	6,100	6,900	0.6	1.1	1.1	0.2	0.3	0.3	150	CO	700
25	Timing test at 10,000 amperes 3,300 volts									150	CO	
26*	Laboratory circuit on phase 2 failed four cycles after breaker closed the circuit; consequently the breaker was not called upon to interrupt appreciable current									150	CO	2,080
27†	8,300	7,700	5,900	1.1	1.8	1.8	0.4	0.7	0.7	150	O	700
28†	6,500	8,400	6,900	1.5	2.5	2.5	0.5	0.8	0.8	150	O	700
29†	25,000	28,000	20,000	2.1	1.1	2.1	0.8	0.4	0.8	150	O	1,170
30†	60,000	71,000	56,000	2.0	2.0	1.8	0.5	0.5	0.4	150	O	1,750
31†	74,000	87,000	77,000	3.0	4.1	4.1	0.7	0.8	0.8	150	O	2,080

* Currents closed (amperes)

	Phase 1	Phase 2	Phase 3
Crest values	160,000	247,000	197,000
Rms values	95,000	150,000	113,000

† In tests 27–31 the line leads were connected to the lower terminals of the breaker, and the upper terminals short-circuited.

difficult for crank-case oil to be carried into the air line.

Auxiliaries in the air-supply system consist of safety valves and two automatic checks to prevent air from either storage reservoir flowing backward to the compressor, necessary gauges, automatic switch to start and stop the compressor, and an automatic alarm valve which provides an indication if the pressure drops due to lack of compressor operation or serious leakage in the system.

Air auxiliaries incorporated as a part of the circuit breakers themselves consist of an additional filter to prevent scale, and so on, from the line entering the breaker tank, an automatic two-way check valve which prevents flow of air from the breaker back into the line and also automatically closes in the event of too large an air flow to the breaker, which could be occasioned by exceptional breaker leakage. An alarm valve and automatic lockout valve are incorporated as part of the breaker to prevent its operation in case the breaker pressure becomes too low. A safety valve and gauge are also included on each breaker tank.

The breaker tank contains sufficient air for two-breaker operations without obtaining additional supply from the main system.

V. Test Results

The breaker was tested with the high-power laboratory facilities described in the MacNeill–Batten paper,[3] using two parallel generators at 13.2 kv, 60 cycles. Complete tests have been made, both single-phase and three-phase.

Tables I and II show a series of 31 consecutive tests made on an interrupter of this type. The tests were all three-phase, 13.2-kv, both opening and closing–opening, and with current values varying from 6,000 amperes to 105,000 amperes. The test with the highest current of this series, 105,000 amperes, was made with a tank pressure of 125 pounds per square inch and on a circuit with a rate of rise of recovery voltage of 2,080 volts per microsecond.

The maximum current closed was 247,000 amperes crest which had an rms value of 150,000 amperes. In nine of the 31 tests, the power interrupted exceeded 1,500,000 kva. For the 31 tests, the maximum arcing time for any phase did not exceed 0.8 cycle, which due to asymmetry of the current wave means that the breaker never failed to clear the circuit when the contacts were more than a small fraction of an inch apart.

Table III. Single-Phase Tests to Determine Effect of Recovery Voltage

Voltage (Kv)	Current Interrupted (Amperes RMS)	Natural Frequency (Cycles Per Second)	Circuit Voltage-Recovery Rate (Volts Per Microsecond—at Test Voltage)	Arcing Time (Cycles)	Inches Contact Separation at Interruption
13.2	8,000	200,000	13,600	0.8	2.5
13.2	8,000	200,000	13,600	0.7	1.5
13.2	8,000	200,000	13,600	0.8	2.3
13.2	8,000	200,000	13,600	0.5	1.4
13.2	8,000	60,000	3,900	0.6	1.4
13.2	8,000	60,000	3,900	0.8	2.3
13.2	8,000	60,000	3,900	0.5	1.2
13.2	8,000	60,000	3,900	0.9	2.6
13.2	8,000	13,000	1,000	0.2	0.4
13.2	8,000	13,000	1,000	0.6	1.4
13.2	8,000	13,000	1,000	0.6	1.3
13.2	8,000	13,000	1,000	0.3	0.6
13.2	8,000	10,250	800	0.3	0.6
13.2	8,000	10,250	800	0.6	1.3
13.2	8,000	10,250	800	0.2	0.3
13.2	8,000	10,250	800	0.4	0.9
13.2	8,000	10,250	800	0.7	1.8
13.2	8,000	3,900	300	0.6	1.3
13.2	8,000	3,900	300	0.4	0.8
13.2	8,000	3,900	300	0.4	0.8
13.2	33,000	100,000	4,130	1.5	5.3
13.2	35,000	100,000	4,130	1.4	6.3
11.0	30,000	24,000	1,370	0.5	1.7
13.2	37,000	24,000	1,640	1.1	4.2
11.0	53,000	240,000	7,100	0.4	1.3
11.0	52,000	32,000	2,390	0.9	4.2
12.0	76,000	32,000	2,390	1.2	8.0

Tables I, II, and III are the results of a series of 58 consecutive tests made without any maintenance on the breaker. Toward the end of the series the first two splitters in the arc chute were eroded sufficiently to lose some of their effectiveness, as evidenced by an increased variation in arcing time. The tests included in Table III, although valuable in indicating the life of the chute, are not as typical of the single-phase performance of the breaker in service as an earlier series of nine consecutive tests which was started with a fresh set of splitters and which gave the data presented in Table IV.

Table IV. Single-Phase Interrupting Tests on a Westinghouse Compressed-Air Circuit Breaker

4,000 Amperes, 15 Kv; 2,500,000 Kva Tested at 150 Pounds Per Square Inch Tank Pressure

Voltage (Kv)	Current Interrupted (Amperes RMS)	Natural Frequency—(Cycles Per Second)	Circuit Voltage-Recovery Rate (Volts Per Microsecond—at Test Voltage)	Arcing Time (Cycles)	Inches Contact Separation at Interruption
10,000	5,000	9,800	570	0.2	0.7
10,000	22,400	22,800	1,320	0.4	1.6
10,000	39,000	25,300	1,470	0.3	1.2
10,000	52,000	27,400	1,590	0.4	1.5
10,000	60,000	27,400	1,590	0.25	1.0
10,000	71,000	27,400	1,590	0.5	2.0
12,000	94,000	27,400	1,910	0.5	2.2
12,000	82,000	27,400	1,910	0.7	3.0
12,000	84,000	27,400	1,910	0.6	2.8

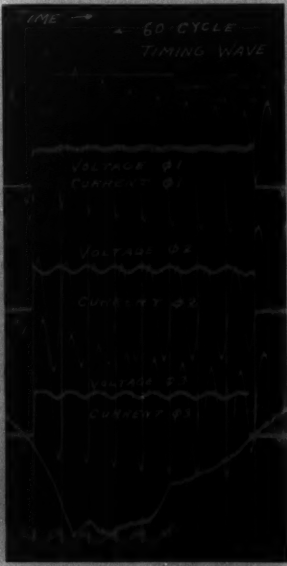

Figure 4. Magnetic oscillogram of typical closing–opening operation test

Table I

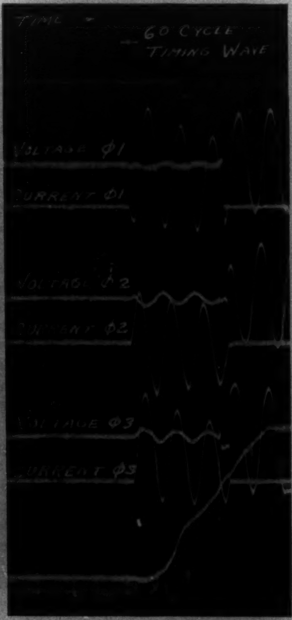

Figure 5. Magnetic oscillogram of an opening operation

Test 10, Table I. 13.2 kv. Current interrupted 91,000, 73,000, and 90,000 amperes

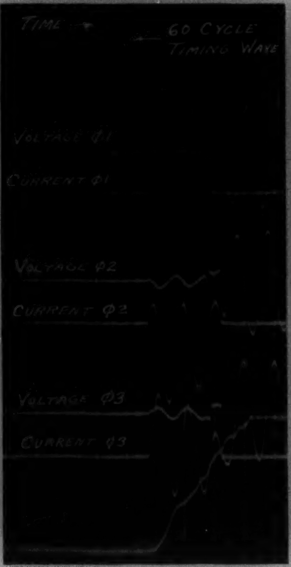

Figure 6. Magnetic oscillogram of an opening operation

Test 21, Table II. 13.2 kv. Current interrupted 102,000, 98,000, and 80,000 amperes

Figure 4 shows the oscillogram for test 7, Table I, which was a closing–opening operation interrupting 48,000, 49,000, and 49,000 amperes for the three phases, and closing in on 150,000, 95,000, and 148,000 amperes crest or 92,000, 59,000, and 86,000 amperes rms respectively.

Figure 5 shows the oscillograms for test 10, Table I, which was an opening operation, interrupting 91,000, 73,000, and 90,000 amperes respectively for the three phases.

Figure 6 shows the oscillogram for test 21, Table II, which was an opening operation, interrupting 102,000, 98,000, and 80,000 amperes for the three phases.

Figure 7 shows the oscillogram for test 22, Table II, also an opening test, with 105,000, 93,000, and 82,000 amperes interrupted for the three phases respectively. For this test the initial pressure in the tank was lowered to 125 pounds per square inch.

The ten tests recorded in Table I were made to demonstrate the breaker to the engineers representing the Consolidated Edison Company. This was the first witness test of an interruption of a three-phase fault approximating 2,500,000 kva.

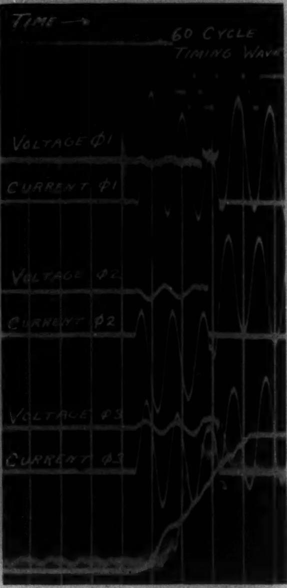

Figure 7. Magnetic oscillogram of an opening operation

Test 22, Table II. 13.2 kv. Current interrupted 105,000, 93,000, and 82,000 amperes

Figure 1B shows the condition of the splitters, coolers, and contacts following the series of 40 tests as listed in Tables I, II, and III.

The first two splitters showed erosion. The coolers were blackened slightly, and the arcing contacts were pitted; however, the breaker was still capable of carrying current and performing further interrupting duty.

The external demonstration on all of these tests was negligible except for some smoke from the grille in the gas-receiving chamber. The noise was held to acceptable limits by the muffler system previously described.

During the development of the 2,500,000-kva breaker, a total of 140 successful interrupting tests was made which were in excess of 1,500,000 kva. The function of such a large number of tests was to study design variations, various operating pressures, and different types of circuit-recovery transients. It was found that the problem of obtaining satisfactory interruption of 2,500,000 kva was one of considerable magnitude, even when a proven breaker of 1,500,000 kva was already available.

VI. Effect of Circuit-Recovery Rate

In powerhouse service generator short-circuit currents would often exceed the capacity of circuit breakers if reactors were not used to limit these currents. These reactors are often placed near the circuit breaker, and this practice generally leads to calculated circuit voltage-recovery rates which are very high.

In case no reactor is used, the highest circuit voltage-recovery rate will be established by the natural frequency of the generators. Generally a single-frequency transient only is involved and the circuit recovery rate may reach approximately 2,500 volts per microsecond.

When the reactor is introduced, its natural period as well as the natural period of the generator is involved, and double-frequency recovery transients result. A natural frequency of the generator may be approximately 30,000 cycles, but the natural frequency introduced by the reactor

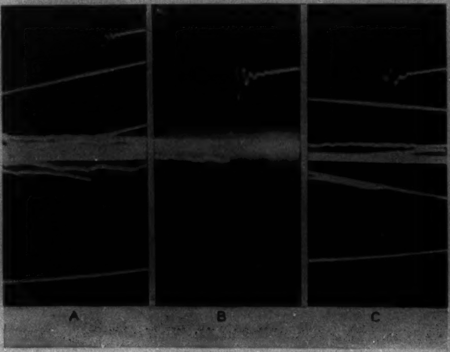

Table III. From left to right:

(A) 13.2 kv, 35,000 amperes, 100,000 cycles per second. 4,130 volts per microsecond

(B) 11.0 kv, 53,000 amperes, 240,000 cycles per second. 7,100 volts per microsecond

(C) 12.0 kv, 76,000 amperes, 32,000 cycles per second. 2,390 volts per microsecond

Figure 8. Cathode-ray oscillograms corresponding to three single-phase tests

Linear Couplers for Bus Protection

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Synopsis: A scheme of bus protection offering advantages in simplicity, speed, and size uses linear couplers (air-core mutual reactances) in place of current transformers. This solves the troublesome problem of saturation and provides a linear relationship between secondary voltage and primary current. The coupler secondaries for a given bus are connected in a series loop with the relay. When the currents entering and leaving the bus are equal, the net induced voltage in the relay loop is zero. For a fault on the bus, however, the net induced voltage, proportional to the fault current, operates the relay. The problems are:

1. To utilize effectively the smaller available energy.

2. To build couplers of sufficiently equal mutual reactance and unaffected by stray fields.

A toroidal coil solved the latter problem. Thorough tests have shown that the performance is strictly linear with respect to primary current, practically unaffected by the primary d-c transient, and thus can be calculated accurately and simply.

THE linear coupler transformer is a constant mutual reactance connecting the primary circuit to the relay. It introduces a new principle into the protective relaying art, a principle* that is fundamentally sound and that eliminates completely, at its source, the most troublesome problem that has been standing in the way of simple high-speed bus protection. That problem is saturation of the current transformers by the d-c transient current that flows for a number of cycles after the occurrence of a fault. Its solution consists of dispensing with the iron, a solution that appears so obvious on the face of it that it is fair to inquire why it was not adopted years ago. This paper might stop right here were it not for the answer to the last question.

As most frequently occurs, there were a number of obstacles to be hurdled between the quite obvious idea of leaving out the iron, and the completion of a successful bus-protective system using linear coupler transformers. Efficient coupler designs had to be developed, capable of deriving the maximum amount of energy obtainable from the available space without the use of iron. New methods had to be devised to use effectively the lower energy level inherent in the elimination of the iron. This energy is adequate for the operation of an efficient a-c plunger-type element in most cases. Also, the development of the copper oxide rectifier to a highly reliable state during the last several years has made available in a-c circuits the sensitive and reliable action of the d-c polar-type of relay. This has greatly extended the range of sensitivities possible with ironless transformers.

New circuits were necessary, better suited than the conventional bus-differential circuits, for use with the accurately linear mutual reactance. Here a new principle* was applied—a radical departure from the principle of the conventional current transformer.

The influence of external fields and positional effects of the primary conductor on

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Paper 42-40, recommended by the AIEE committee on protective devices for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 24, 1941; made available for printing December 23, 1941.

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The authors acknowledge the assistance of C. A. Woods in connection with the bushing-type couplers, and of other associates who have contributed to the success of this project.

* The principle of perfect linearity between primary current and secondary induced voltage in couplers as described.

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may reach 200,000 cycles. Circuit recovery rates based on the first crest of the recovery transients may be as high as approximately 12,000 volts per microsecond.

The performance of the 2,500,000 compressed-air breaker has been studied with a wide variety of recovery-voltage conditions. A number of the important tests are summarized in Table III. The first part of the table lists a series of interruptions at 13,200 volts single phase and 8,000 amperes. The circuit-recovery rate was varied from 300 volts per microsecond to 13,600 volts per microsecond. To obtain the high recovery transients, a reactor was placed directly in the test cell with only a few feet of cable between it and the breaker. Interruption was satisfactory in all cases.

The second portion of the table lists tests made at higher currents. To obtain these the laboratory circuit capacitance was reduced, as much as practical, and during several of these tests double-frequency recovery transients were obtained. Three cathode-ray oscillograms of these interruptions are shown in Figure 9. One at 53,000 amperes was obtained with a circuit adjusted for 7,100 volts per microsecond. The second was obtained at 35,000 amperes with the circuit adjusted for 4,130 volts per microsecond, and the third was obtained at 76,000 amperes with the circuit also adjusted for 2,390 volts per microsecond. All three of these circuit conditions represent double-frequency transients, but in the second and third oscillograms the higher frequency is so greatly damped by the conduction current of the breaker following current zero that little trace of the oscillation appears in the recovery-voltage transient recorded by the oscillograph.

These are the highest published voltage-recovery rates measured on circuits on which heavy short-circuit tests have been made.

VII. Conclusions

The complete test results show that the 2,500,000-kva compressed-air breaker, which is the largest air powerhouse breaker ever built, is entirely adequate in interrupting capacity even in the case of extremely high-circuit voltage-recovery rates. The small external demonstration, ease of maintenance, complete freedom from fire hazard, and general mechanical simplicity indicate that there will be a growing tendency to utilize this type of breaker for indoor powerhouse installation.

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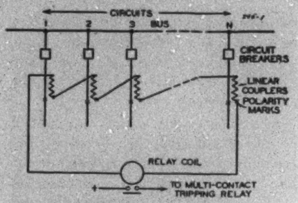


Figure 1. Schematic one-line diagram of connections showing application of linear couplers and relay to bus protection

the response had to be dealt with. This was done not by brute force, but by bringing into play a useful theorem relating to coils wound on nonmagnetic cores of toroidal or ring shape.

Methods had to be devised for winding toroidal coils conforming to the requirements for efficient energy utilization and high accuracy without undue economic limitations.

Thus, it is not simply the elimination of iron in a transformer but rather the technique of using linear couplers in relaying circuits and presentation of the verifying tests which have necessitated this paper.

To more fully understand the place filled by the linear-coupler scheme of bus protection, mention should be made of certain other schemes in current use. Bus-protection schemes that have received the most attention in recent years have revolved about some form of current-differential protection. The relays considered have varied from simple overcurrent relays[1] to various forms of restrained relays. Restrained relays have utilized either the fault current itself,[2] or harmonics in the differential current.[3] Another method of attacking the problem from the standpoint of directional comparison[4] was presented at the 1941 summer convention.

Impedance and reactance schemes,[6] measuring the impedance from the main incoming sources into the feeder reactors have also found considerable application where reactors are used and ratios of fault impedance to reactor impedance are such as to provide discrimination. However, they are necessarily restricted to certain busses which happen to fill these requirements. The fault-bus scheme[1] is also ideal for certain new installations that can be arranged to accommodate it.

Table I. Greatest Allowable Ratio of Maximum Through Fault* to Relay Setting for 2-to-1 Safety Factor**

Coil Mutual Reactance Tolerance (Per Cent)	Ratio of Maximum Through Fault to Relay Setting
±5.0	5:1
±2.5	10:1
±1.5	17:1
±1.0	25:1
±0.25	100:1

*Rms symmetrical.

**Larger ratios can be covered with proportionately reduced safety factors.

In all of the current-differential schemes, the over-all performance necessarily depends upon the performance of the current transformer. For busses near large generating stations, the presence of the d-c component in asymmetrical fault currents has greatly magnified the problem through its saturating effect[7] on the current transformers.

Another related problem is the large ratio of maximum external fault current when the relay should not trip to minimum internal fault current when the relay should trip. A large ratio between these values is encountered in those stations where the neutral is grounded through an impedance, thus materially limiting the minimum phase-to-ground fault current for internal faults without limiting the maximum phase-to-phase and three-phase fault current for external faults.

The very sensitive relay setting required under this condition greatly magnifies the problem created by d-c saturation at high overcurrents. The problem has been solved in some instances by the use of extremely large current transformers designed not to saturate on the d-c component. What is felt to be the most practicable scheme to date utilizing current transformers of standard proportions is the multirestraint variable percentage scheme.[2] In this scheme particular attention is given to providing a sufficient number of restraining elements, actuated by the secondary fault current. Also, the principle of variable percentage characteristics is utilized by which means the relay sensitivity is reduced at the higher currents where current transformer performance is poorest. By this means a range of 100/1 between maximum external fault current and relay setting may be covered without fear of false operation. An operating time of three to six cycles is secured through the use of the high-speed induction-type relay.

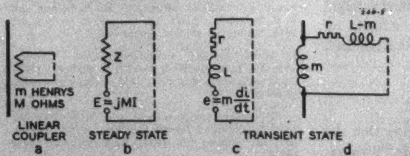


Figure 2. Basic equivalent circuits of linear coupler

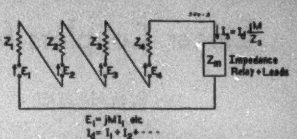


Figure 3. Series-connection equivalent circuit of linear couplers

Heretofore, the problem has been attacked from the standpoint of either making the current transformers more nearly perfect, which is a costly method, or of designing the relay to meet the limitations of the current transformers. It is immediately obvious that if the current transformers were perfect in their response a simple overcurrent relay differentially connected would be all that would be necessary. One line of attack which has been recently introduced involves the use of a special current transformer with air gaps in the core.[8] This arrangement can be made high speed but cannot cover the 100-to-1 range of the multirestraint variable-percentage scheme. The authors state its range as approximately 10 to 1.

The linear-coupler scheme described herein is essentially a high-speed scheme covering a range at the present state of development of 17 to 1 with a 2-to-1 factor of safety. Tests have shown this rating to be quite conservative.

Summary

Linear couplers are distinguished from current transformers in having a constant mutual impedance even when the entire primary current acts as exciting current, with no current in the secondary. It is the function of a current transformer to produce in its secondary a miniature replica of the current that flows in its primary. It is the function of a linear coupler to produce in its secondary an internal voltage proportional to the current in its primary. To form a differential circuit, these voltages are added in series (Figure

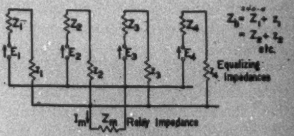


Figure 4. Parallel-connection equivalent circuit of linear couplers

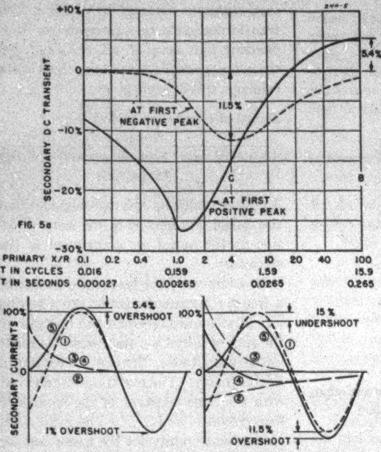


Figure 5. D-c transient current in linear-coupler secondary circuit when primary current is initially fully offset

Drawn for $X_s=r_s$. First positive peak taken at 0.375 cycle and first negative peak at 0.875 cycle from instant of fault since secondary current leads primary current by 0.125 cycle

5a. Secondary total d-c transient component, in per cent of a-c component positive crest, with respect to primary d-c time-constant, T, or 60-cycle X/R ratio

5b. Condition B of part (a), $X/R=100$, $T=15.9$ cycles

5c. Condition C of part (a), $X/R=4$, $T=0.64$ cycle

(1). Secondary a-c component current
(2) Forced d-c transient component
(3) Free d-c transient component
(4) Total d-c transient component
(5) Resultant secondary current

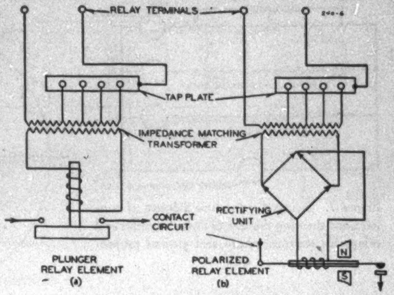


Figure 6. Schematic internal diagram of relays

(a) Simple plunger-type element
(b) Sensitive polar-type element

1), as distinguished from current transformers in which the currents are added in parallel. For an external fault the vector sum of all the primary currents is zero. Thus, the vector sum of all induced voltages is zero, and also the relay current is zero. For an internal fault the sum of the primary currents through legitimate circuits is equal to the fault current. Consequently, the vector sum of all induced voltages is proportional to the fault current. The current in the relay is equal to the vector sum of the voltages acting around the relay loop divided by the loop impedance. Thus, this current is also proportional to the fault current.

A one-cycle relaying scheme including couplers and relays has been developed, operating on the principle just outlined, and has been subjected to an exhaustive series of tests. Through type couplers of 0.005 ohm mutual reactance were built in several sizes and shapes to fit in the usual bushing current-transformer compartments or for separate mounting.

Sufficient energy is obtainable from couplers of this proportion to operate an a-c plunger-type relay for fault currents down to 1,500 amperes with a six-circuit bus and 2,000 amperes with a ten-circuit bus. Couplers of reasonable cost can be built to the desired mutual reactance within a tolerance of ±1.5 per cent, thus permitting a maximum false differential of three per cent. Taking a 2-to-1 factor of safety, this results in a minimum allowable setting of six per cent or $^1/_{17}$ of the maximum through fault current. The sensitivities obtained are ample for busses where the setting does not need to be below $^1/_{17}$ of the maximum bus fault.

The tests have demonstrated this simple and straightforward scheme to be thoroughly reliable and practically free from transient effects, so that the performance can be readily calculated. Operation under one cycle was obtained for practically all internal faults and in no cases were operations obtained on external faults within the limits prescribed for proper application.

Tests have verified the feasibility of testing an actual bus installation with low steady-state currents to determine the performance to be expected under fault conditions.

The following paragraphs include an outline of the theory of the linear-coupler scheme, a description of the apparatus employed, and a résumé of the combination tests which have been conducted to verify the theory and prove the equipment designs.

Theory of Linear-Coupler Circuits

NOMENCLATURE

M, m—Mutual reactance and inductance between the primary conductor and secondary winding of a linear coupler

$Z=r+j\omega L$—Secondary self-impedance of a coupler

$Z_s=r_s+j\omega L_s=r_s+jX_s$—Total impedance of coupler-relay loop circuit

E—Induced voltage in coupler

E_s—Vector sum of induced voltages in coupler-relay loop

I, i—Primary current, vector, and instantaneous values. Subscripts 1, 2, 3, and so on designate the various bus circuits

I_s—Secondary current in the relay, vector value

I_f—Fault current, into the bus for an internal fault; into and out of the bus for a through fault

I_d—Vector sum of primary currents; equal to I_f for internal faults, and zero for external faults

EQUIVALENT CIRCUIT

As illustrated in Figure 2b, the equivalent circuit of a linear coupler is simply an internal voltage of value jMI, in series with an impedance of value Z. The equivalent circuit for transient conditions, Figure 2c, is of the same form. However, the internal voltage is then mdi/dt. An alternate form convenient for determining the transient response is also shown in Figure 2d.

As will be shown later, the transient response of the couplers considered is negligible so that the steady-state response can be used for analyzing fault conditions. Thus, for calculations, the series connection shown in Figure 1 may be treated through its equivalent circuit, Figure 3.

SERIES CONNECTION — STEADY - STATE CONDITIONS

The total voltage acting around the series circuit, Figure 3, is the vector sum of the individual voltages shown. If the mutual reactance, M, is the same for each coupler, the total voltage is equal to this mutual reactance, times I_d, the vector sum of the currents flowing into the bus.

For *internal fault* conditions, the I_d cur-

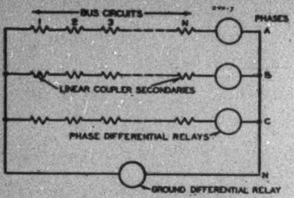

BUS CIRCUITS
1
2
3
N
PHASES
A
B
C
N
LINEAR COUPLER SECONDARIES
PHASE DIFFERENTIAL RELAYS
GROUND DIFFERENTIAL RELAY

Figure 7. Schematic one-line diagram of connections showing the use of linear couplers and relays for separate phase and ground protection

rent is equal to the fault current I_f, and the voltage induced in the loop is jMI_f. The secondary current flowing in the relay is obtained by dividing this induced voltage by the relay loop impedance. That is, for internal faults,

$$I_s = I_f(jM/Z_s) \tag{1}$$

For *external fault* conditions since the vector sum of the currents flowing into the bus is zero, the net voltage induced in the loop circuit is zero, and consequently the relay current is zero. If the incoming and outgoing mutual reactances differ by three per cent, a relay current is obtained having a value three per cent of that which flows for an internal fault of the same magnitude. Thus, with couplers built to a standard mutual reactance within ±1.5 per cent, the mutual reactances of the incoming and outgoing couplers might differ by three per cent under the worst conditions. In order to maintain a 2:1 factor of safety, the relay should not be set below six per cent of the maximum through fault, since current values of three per cent are possible during external fault conditions when the relay should not operate. Table I shows the mutual reactance tolerances required to permit different ratios of maximum through fault to relay setting with a 2:1 safety factor.

Series Connection—Transient Conditions

Internal Faults. An asymmetrical primary current wave contains an a-c component and a d-c transient component, each of which produces a counterpart in the secondary loop circuit, having impedance $Z_s = r_s + j\omega L_s = r_s + jX_s$.

Figure 8. Type *LC* linear-coupler transformer

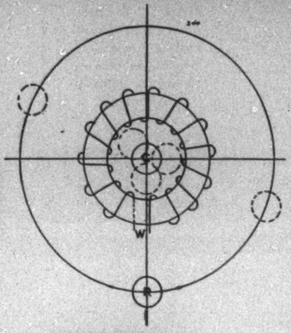


Figure 9. Positional effects of inner and return conductors on coupler winding

As the center conductor is moved into the various possible positions, the mutual inductance will vary slightly. This variation is eliminated in a design with fixed primary bar

As the return conductor is moved along the arc of a circle, the mutual inductance of it with respect to the torodial winding will vary, and will be substantially zero at two points

The secondary a-c component amplitude is M/Z_s times that in the primary, as shown for the steady-state analysis.

A primary d-c component of time constant, T, produces a secondary forced d-c component of the same time constant. Referring to Figure 2d, a current of time constant, T, encounters mutual impedance $-m/T$, secondary branch impedance $r_s-(L_s-m)/T$, and sum of branches or secondary loop impedance r_s-L_s/T. Hence, the initial forced d-c component in the secondary relay branch is $-(m/T)\div(r_s-L_s/T)$ times that in the primary.

For a primary current wave initially fully displaced, the initial primary d-c component is equal to the negative amplitude of the primary a-c component, and therefore, the ratio of their secondary components is obtained by dividing their transformation factors, given above. Recognizing that $\omega m = M$, $\omega L_s = X_s$ and that the components have opposite signs in the primary, this division gives:

$$\frac{\text{Forced d-c transient initial magnitude}}{\text{Secondary a-c component amplitude}} = \frac{Z_s/r_s}{\omega T-(X_s/r_s)}$$

A positive value for this ratio signifies that the forced transient is in the same direction as the initial a-c component in the secondary.

In addition to the forced d-c transient, a free d-c transient current flows having an initial amplitude equal and opposite to the sum of initial a-c and forced transient initial amplitudes, thereby preventing a discontinuity. The free transient dies out with the time constant of the secondary loop circuit, $T_s = L_s/r_s$. The ratio X_s/r_s approximates unity for the linear-coupler circuits, resulting in T_s approximately 0.00265 second or one-sixth of a cycle.

The forced and free d-c transients decay and the a-c component reverses, as shown in Figures 5b and 5c until somewhat less than one-half cycle after the fault a positive peak is reached, which may be more or less than the steady-state value, depending on the magnitude and direction of the total d-c transient. Progressing further, the first negative peak is reached, and it can be greater than the steady a-c value if the total transient is negative at this point.

The values of the total d-c transient at the first positive and first negative peak are shown in Figure 5a for the X_s/r_s ratio involved in linear-coupler circuits. The maximum overshoot of the first positive peak occurs for a long primary time constant. It is 5.4 per cent for a time constant of 15.9 cycles as illustrated in Figure 5b. The maximum overshoot of a negative peak occurs for a short time constant of about 0.64 cycle (condition *C*, Figure 5a) and is 11.5 per cent. It is illustrated

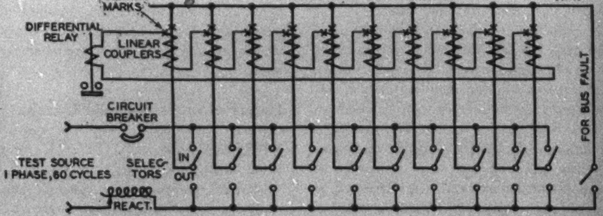


Figure 10. Arrangement of test bus for ten primary circuits and test diagram of primary and secondary circuits

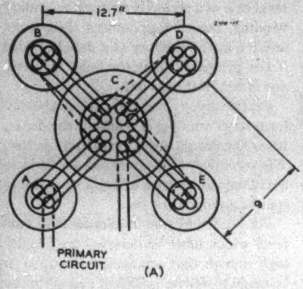
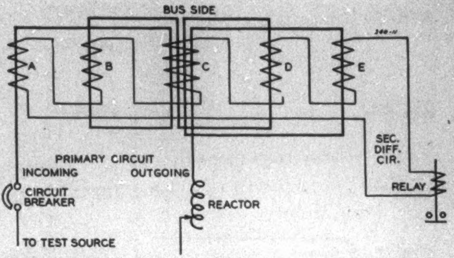


Figure 11. Five-circuit bus test diagram and arrangement to obtain long d-c time constants in fault current transient

A. Physical arrangement of couplers and primary winding

B. Test diagram of primary and secondary circuits



in Figure 5c. Succeeding cycles involve much less overshoot. In the range of time constants usually involved, neither overshoot is much over 5 per cent, as was verified by the tests. Practically this transient is negligible in making relay settings.

For larger ratios of X_s/r_s much larger percentage overshoots occur on the first cycle. Kennedy and Sinks[6] show 60 to 80 per cent overshoot at the first positive peak, with air-gap-type current transformers.

External Faults. The amount of overshoot is of particular importance in connection with high-speed relaying since the false differential through the relay during through faults is increased in direct proportion to it.

PARALLEL CONNECTION

The current in the relay for the parallel connection has been given.[5] With the nomenclature of Figure 4 it is

$$I_m = I_d \frac{jM}{Z_b + nZ_m}$$

where n is the number of circuits, that is, $n=4$ for the four-circuit bus, Figure 4. This current is zero for external faults and is a definite proportion of the fault current for internal faults. For this connection, it is necessary to have all secondary branch impedances equalized to a common value Z_b. Resistance differentials due to temperature differences must, therefore, be considered. Assuming a relay matched to the parallel impedance of the couplers, a 30-degree centigrade temperature differential between incoming and outgoing circuits would cause approximately eight per cent false differential on through faults.

The parallel connection has the advantage that it would trip correctly for an internal fault even with an inactive coupler open circuited. However, it is subject to the temperature errors mentioned above, and is not as readily supervised as the series loop arrangement. Also, even with equal mutual reactances the parallel connection involves the flow of secondary a-c and transient currents during through faults, whereas the series connection involves none. The simpler series loop arrangement is, therefore, preferred.

Relaying Schemes Using Linear-Coupler Transformers

A schematic one-line diagram of connections utilizing linear couplers and a sensitive relay is shown in Figure 1. The requirements of a suitable relay to be utilized in this scheme are as follows:

1. Since the amount of energy available is small, the relay must be quite sensitive. That is, the volt-amperes consumed by the relay at minimum pickup must be as low as possible.

2. Because of the small amount of energy available, the impedance of the relay should be matched to that of the linear couplers in order that maximum efficiency may be obtained. This involves making the relay impedance equal to the series impedance of all of the couplers with which it is used. There are variations in the self-impedances of the various coupler designs depending principally upon the space available. Also, there are variations in the number of circuits to a bus depending upon the application. For this reason the relay should have taps so that its impedance may be approximately matched to that of the linear couplers to suit the application.

3. Since the relay normally operates with no restraint at all, it should be shockproof.

A schematic internal diagram of the relay wiring is shown in Figure 6. Taps are shown on the primary of a transformer to provide the necessary impedance-matching characteristic. With a given setting on the relay element, the use of a tapped primary winding on the transformer supplying this element will obviously change the minimum tripping current of the relay as expressed in terms of current in the tapped winding. However, this does not change the amount of energy required to operate the relay as expressed in volt-amperes.

The minimum primary current upon which the scheme shown in Figure 1 will operate is determined by the mutual impedance of the linear couplers together with the total impedance of the secondary circuit, and the minimum pickup energy required by the relay. In any given application the minimum value may be determined from the known constants of the circuit by the simple calculations indicated by equation 1.

In determining upon a suitable relay design for this application, requirement 1 as listed above requires the most attention. Providing impedance taps as listed under requirement 2 is a simple matter. In making tests of the over-all scheme, two relay designs were provided which are not necessarily the last word. The first of these involves a simple plunger-type of overcurrent element of medium sensitivity which is inherently shockproof. A second relay was built around a polar-type relay element. This element required the use of a rectifying unit for its operation and operates reliably at very low energy. The sensitivity of the two elements is dealt with more fully under test results, but it is of interest to note here that for each relay the minimum pickup current as expressed in primary amperes was calculated with gratifying accuracy.

Separate Ground Relay. At the present time linear couplers have not been manufactured which would be sufficiently accurate to permit their use in an application where the range of maximum external fault current to minimum internal fault current is as great as 100/1, as sometimes occurs on high-impedance grounded systems. A possible means of expanding the range involves the use of a separate ground relay. The schematic diagram of connections for this is shown in Figure 7. This scheme anticipates the use of a ground relay set sufficiently low to detect the minimum internal ground fault. However, means must be taken to prevent the relay from operating for heavy external interphase faults. In other words, at the current magnitude experienced for heavy interphase faults, the variation in response of linear couplers may be suffi-

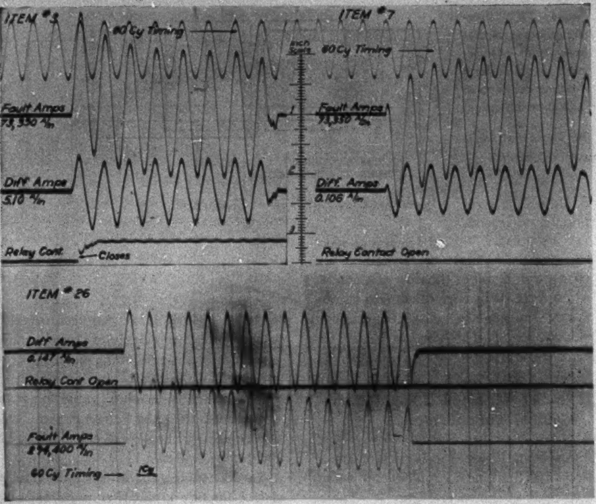


Figure 12. Oscillograms of pertinent fault tests

(a) For item 3 test, a 55,200-ampere bus fault showing high-speed one-quarter-cycle operation of plunger-type relay

(b) For item 7 test, a 50,800-ampere a-c component through fault with the couplers deliberately adjusted to give maximum error differential. It shows 1.61 per cent differential and no operation of the plunger-type relay (set to respond for a 2,450 ampere bus fault, 4.83 per cent of the 50,800-ampere through fault). While the fault current was initially 95 per cent offset with a short 1.2-cycle d-c time constant, the differential current shows negligible transient effect

(c) For item 26 test, a 125,000-ampere a-c component through fault initially 95 per cent offset with a long 8.2 cycle d-c time constant. It shows negligible transient effect in the differential current. The 0.89 per cent false differential did not operate the relay (set to respond for a 1,820-ampere bus fault, 1.45 per cent of the 125,000-ampere through fault)

cient to cause operation of the ground relay because of its sensitive setting.

The separate ground relay will be used only in those stations where there is an impedance in the station neutral connection limiting the phase-to-ground fault current on the bus to a value too low for the phase relays to detect.

The Linear Coupler Design and Construction

The linear coupler is simply an air-core mutual inductance, and as such may be designed and built in any of a large number of ways. The choice of method was selected after study of the required properties of the coupler:

(*a*) Accurate constant mutual inductance with respect to the primary circuit.

(*b*) Negligible mutual inductance to any external or neighboring circuit.

(*c*) A sufficiently high ratio of mutual inductance to internal impedance to obtain the power output required for the relay.

Requirements (*a*) and (*b*) practically demand that the secondary winding be a toroidal or ring winding, as this type of winding is practically without mutual inductance to external circuits and has constant mutual inductance to the primary circuit. Current in a secondary winding, uniformly wound on a nonmagnetic toroidal core approaches a uniform current sheet of finite thickness which produces only circular flux lines confined entirely within the winding and core. All this flux links a conductor passing anywhere through the opening of the core, and none of it links a conductor not passing through no matter how close it may be. The presence of neighboring iron outside of the winding has no influence since there is no magnetomotive force outside of the current sheet. Thus, the secondary winding has a definite mutual reactance with respect to a linking conductor and zero mutual reactance with respect to a conductor that does not pass through the opening.

While the mutual reactances have been visualized in terms of the flux linking the primary for current in the secondary, the relationship is reciprocal. Thus, for the ideal case of a perfectly uniform secondary winding, the voltage induced therein per ampere in the primary is a definite value if the primary conductor links the core, and is zero if it does not.

Practically, a close approach to the uniform sheet winding is realized, the departures therefrom being evaluated by test. The secondary is wound back on itself to avoid any single-turn effect of progressive spiraling around the core.

For bus protection, minimum fault currents which must be detected are usually high enough that economical designs can be made to deliver sufficient energy to the relay when only one primary turn is used. The practical design for bus protection, therefore, usually takes the general form of a through-type current transformer, with an air-core ring winding. Figure 8 shows a type for separate mounting.

The calculation of the mutual inductance and output capacity of the device is very easily worked out according to well-known fundamental formulas. The principal manufacturing problem is to make ring windings which are sufficiently perfect

1. To be free from induction from neighboring conductors.
2. To have a mutual inductance which departs from the exact required values by only a sufficiently small per cent for all possible positions of primary conductor.

These two requirements may each be expressed in terms of the variation from the desired value. The actual coupler will have a definite, though very small mutual inductance with the return conductor, which may be expressed in per cent of the mutual inductance to the primary conductor. This inductance will vary with the spacing and angular position of the return conductor, according to Figure 9, and will usually be zero for two positions and maximum for two other positions.

A final source of variation is error in calibration of the mutual inductance. In order to obtain mutual inductances within the necessary tolerances, adjustments are required after winding the coils. The linear couplers are tested by balancing them against a standard mutual inductance.

Considering all of the sources of variation as mentioned above, it appears quite practical at the present time to control the mutual reactances of couplers within ±1.5 per cent in commercial production.

Combination Tests

Combination tests were made to prove that it was practical and safe to use a simple overcurrent-type high-speed relay

Table II. Summary—Linear-Coupler Bus Differential Test Results

Item No.	Bus No. of Circuits Total	In	Out	Idle	Fault Type of	I_f Amperes	Induces I_fM Volts	Differential I_s Amperes	I_sZ_s Volts	% Diff.	Relay Type and Pickup Amperes	Times Pickup	Contact in Cycles	Remarks
1	10 Per Figure 10	10	0	0	Internal	2,500	12.5	0.087		100	Plunger 0.085	1.02	5.05	Pickup I_f=2,450 test, 2,430 calculation
2		1	0	9	Pickup	2,560	12.8	0.089		100		1.05	4.30	=2,450 test, 2,430 calculation
* 3		4	0	6	Internal	55,200	276	2.08		100		24.4	0.23	
4		5	0	5	High current	103,600	518	4.20		100		49.4	0.16	
5		5	5	0	External	60,200	301	0.0082	1.08	0.36		0.096	None	Represents average % differential
6		4	1	5	External	57,000	285	0.0056	0.72	0.25		0.066	None	Normal low % differential
* 7		4	1	5	External	50,800	254	0.0310	4.09	1.61		0.365	None	Adjusted for maximum % differential
8	10 Per Figure 10	5	0	5	Internal	532	2.66	0.0180		100	Polar 0.0173	1.04	4.11	Pickup I_f=511 test, 509 calculation
9		1	0	9	Pickup	532	2.66	0.0180		100		1.04	4.97	=511 test, 509 calculation
10		4	0	6	Internal	50,400	252	2.165		100		125	0.64	
11		5	0	5	High current	100,000	500	4.620		100		267	0.64	
12		5	5	0	External	60,200	301	0.0068	1.12	0.37		0.393	None	Represents average % differential
13		4	1	5	External	56,400	282	0.0028	0.66	0.23		0.162	None	Normal low % differential
14		4	1	5	External	48,600	243	0.0240	3.46	1.42		1.39	1.72	Adjusted for maximum % differential
15	6 Modified Figure 10	3	0	3	Internal sensitivity	1,540	7.70	0.1360		100	Plunger 0.132	1.03	4.37	Pickup I_f=1,500 test, 1,530 calculation
16		3	1	2	External	51,200	256	0.0672	3.70	1.44		0.51	None	Adjusted for maximum % differential
17		3	0	3	Internal sensitivity	300	1.50	0.286		100	Polar 0.0279	1.02	5.45	Pickup I_f=294 test, 320 calculation
18		3	1	2	External	50,600	253	0.0688	3.44	1.36		2.47	1.18	Adjusted for maximum % differential
19	2 Modified Figure 10	1	0	1	Internal sensitivity	1,208	6.04	0.1360		100	Plunger 0.132	1.03	5.18	Pickup I_f=1,170 test, 1,140 calculation
20		1	1	0	External	50,600	253	0.1003	4.10	1.62		0.76	None	Adjusted for maximum % differential
21		1	0	1	Internal sensitivity	252	1.26	0.0280		100	Polar 0.0279	1.00	5.75	Pickup I_f=252 test, 246 calculation
22		1	1	0	External	49,200	246	0.1015	4.00	1.63		3.64	0.86	Adjusted for maximum % differential
23	5 Per Figure 11	5	0	0	Internal sensitivity	1,820	9.10	0.1140		100	Plunger 0.114	1.00	Yes	Ammeter test, I_f raised to pickup
24		4	1	0	Induced volts	20,000	100	0	0.89	0.89		0	None	Voltmeter test, secondary differential circuit open
25		4	1	0	External	77,800	389	0.0446	3.57	0.92		0.39	None	97% offset, T_{dc}=8.7 cycles
* 26		4	1	0	External	125,000	625	0.0693	5.54	0.89		0.61	None	95% offset, T_{dc}=8.2 cycles
27		4	1	0	External	216,000	1,080	0.1210	9.68	0.90		1.06	1.65	

*Denotes oscillogram shown in Figure 12.

with linear couplers for differential protection, regardless of the number of circuits in the protected zone. This meant demonstrating that the couplers were linear in fact and practically unaffected by any stray fields produced by other circuits.

A bus setup accommodating 10 circuits, was made for test purposes as shown in Figure 10, in which the primary circuits were spaced at 12-inch centers. Four different types of 0.005 ohm, ±1 per cent linear couplers, having different dimensions, were available. One or more of each type were used in various combinations with their secondary windings in series with the relay to form bus differentials of 10, 6, and 2 circuits. Also, the parallel secondary connection of couplers was tested for a six-circuit bus to prove that the parallel connection was feasible, even if not as desirable as the series connection.

An exhaustive series of tests was made to cover the variations in relay type and sensitivity, in coupler size and shape, in fault-current magnitudes and transients, in fault-current distribution, and in the astatic factors. The astatic effects were checked:

1. By interchanging couplers on the primary circuits.
2. By rotating the couplers on their axis.
3. By placing the couplers off center with respect to their primary conductor.
4. By varying the distance from the coupler to the bus.
5. By turning the coupler upside down.
6. By placing magnetic materials between the couplers and in close proximity.

All these astatic effects were found small for spacings involved in practice.

A special setup per Figure 11, using multiple primary turns, was made to get the effect of large current with a smaller source current in order to obtain increased d-c time constant. It should be noted that each primary turn feeds in through one of the smaller outer couplers and returns through the larger center coupler. This arrangement simulates a five-circuit bus with four equal sources feeding to an external fault through the center coupler.

Test Results

Table II gives pertinent information and results of representative tests for 10-, 6-, and 2-circuit busses per Figure 10 and for the special five-circuit bus per Figure 11.

Items 1 to 7 apply for the 10-circuit bus per Figure 10 using the plunger-relay set to pick up at 0.085 ampere in the coupler secondaries. Calculations based on equation 1 give pickup for internal faults at

2,430 amperes. Test item 1 with 10 circuits energized, and item 2 with only one circuit energized, both give pickup at 2,450 amperes when the fault current is proportioned for unity pickup. Based on ±1 per cent tolerance in the couplers this sensitivity should be safe for through-fault currents of 25 times 2,450 or 61,000 amperes. Items 5 to 7 for external faults show that it was safe as the relay current remained below half pickup value. In items 6 and 7 the fault current into the bus was supplied by four circuits and out from the bus through one circuit with the five remaining circuits "idle" representing feeders with no feed back. Item 6 shows normal low (0.25 per cent) differential and item 7 the maximum (1.61 per cent) differential obtainable after deliberately adjusting the coupler positions for maximum astatic effects. The oscillogram, Figure 12b, for item 7, shows a fault current of 50,800 amperes rms a-c component, I_f, plus 68,300 amperes initial d-c component decaying with a short-time constant of 1.2 cycles; and a secondary differential current, practically free of d-c transient, having an a-c component, I_s, of 0.031 ampere which gave no relay operation. These measured data and additional derived data are given in the tabulation. Based on a 0.005-ohm mutual (five volts induced per 1,000 amperes) the 50,800-ampere fault should induce +254 volts total in the incoming-circuit couplers and −254 volts in the outgoing-circuit coupler, which would leave no differential voltage to circulate secondary current. Actually, 0.0310 ampere flowed which multiplied by the loop impedance, Z_s, indicated a differential of 4.09 volts or 1.61 per cent. The relay current, I_s, equals 0.0310 ampere and represents 0.365 times its pickup value of 0.085 ampere. Oscillogram, Figure 12a, for item 3, shows one-fourth cycle operation of the plunger-type relay for a 55,200-ampere high-current internal fault.

Items 8 to 14 also apply for the 10-circuit bus but show the performance of the more sensitive polar-type relay having a sensitivity of 511 amperes. Items 15 to 18 cover a six-circuit bus arrangement. Items 19 to 22 are for a two-circuit arrangement and were included to show the maximum obtainable differential.

The performance of the five-circuit bus arrangement, Figure 11, for long d-c time constant is shown in items 23 to 27. The fault current I_f tabulated is the equivalent value based on a single primary conductor. For item 23, the center-coupler secondary was deliberately reversed to represent an internal fault and the current increased gradually until the relays just operated at 1,820 amperes. The center-coupler secondary connection was then restored to normal, and the induced and differential voltages measured with the secondary circuit open. These induced voltage tests, item 24, showed 0.89 per cent differential over a wide range of steady-state currents, and this percentage differential was substantiated by subsequent transient-fault tests, items 25 to 27. This, therefore, illustrates the possibility of testing an actual bus installation with low steady-state currents to determine the performance to be expected under fault current. Figure 12c shows the oscillogram for item 26, a 125,000-ampere a-c component through fault (representing 69 times relay sensitivity) which was initially 95 per cent offset, involving a 170,000-ampere d-c component decaying slowly with a time constant of 8.2 cycles or 0.137 second. Again the differential current is not offset but quite symmetrical, and the free transient as shown at the end of the fault lasts only a small portion of a cycle. A comparison with Figure 12b for item 7 shows that the d-c component and its time constant have a quite negligible effect on the differential current. Based on the differential current of item 26, showing 0.61 of relay pickup for a 125,000-ampere through fault, relay operation is expected for through faults in excess of 125,000/0.61 = 205,000 amperes; and item 27 shows relay operation at 216,000 amperes with 1.06 times relay pickup, verifying the 205,000 ampere through-fault pickup.

Summarizing, the primary current required to pick up the relay for bus faults was found to be almost precisely as calculated and showed no perceptible transient effect on pickup or speed of operation as a result of d-c component current. The pickup depended only on fault-current magnitude and was independent of its distribution. For external faults the secondary differential was found to average about one fourth of the tolerance band. It did not exceed the tolerance band even when a deliberate attempt was made to get maximum astatic effect. The tests, therefore, verified the fact that the maximum differential for through faults can be calculated with assurance. Above all, the staged fault tests demonstrated that the d-c transients, which are so troublesome with current transformers because of the d-c saturation effects in the iron, have a very negligible effect on the coupler-differential performance, and therefore, it is quite feasible to test an actual bus installation by circulating low-value steady-state current.

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Facilities for the Supply of Kilowatts and Kilovars

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Synopsis: Increased system kilowatt capacity may be realized by the reduction of generator kilovar requirements and the provision of reactive capacity sources at other points in the system. The factors to be considered in the choice of various reactive sources are discussed. Benefits to be realized from the various reactive sources are described. The paper is based upon system plan studies of a large eastern utility.

IN determining a system's capacity requirements for supplying the electric load—kilowatts and kilovars under all conditions of operation throughout the year—a comprehensive capacity and load study is necessary. This was particularly emphasized by two operating experiences on the system of the Public Service Electric and Gas Company. The first episode was a system voltage disturbance which occurred on October 30, 1938, and the second a system shutdown which occurred as a result of a 132-kv bus fault at Roseland switching station on July 11, 1940. Investigations of these operations introduced the following subjects for consideration:

1. The proper kilowatt and kilovar loading of individual generators, considering their economy, thermal, and stability characteristics.
2. The amount and distribution of various forms of kilovar capacity throughout the system.
3. Certain improvements in system protection, particularly with respect to more rapid fault removal and the maintenance of adequate backup protection.
4. Installation of certain tap-changing-under-load equipment to maintain bus voltages within certain limits and to make all generator kilovar capacity available to the system at all times.
5. And finally a more thorough analysis of the kilowatt and kilovar loads on the system.

Studies have progressed to such a point that a fairly complete report can be made of the conclusions which have been reached. Continuing studies may lead to other equally important conclusions, but, in general, they will probably be only refinements of the conclusions now reached. The application of these conclusions is particularly important at this time with the present scarcity of materials and the necessity for added capacity to meet greatly increased industrial loads.

General Description of System

A description of the Public Service Electric and Gas Company system is necessary as some of the results of the studies presented may hold for this system alone; similar studies would have to be conducted on other systems to reach corresponding conclusions. The layout of this system is not unlike many others but the load for the 1941 hourly integrated peak, which was 852,100 kw, is probably more concentrated than most, being spread over an area of approximately 1,400 square miles. This area is roughly rectangular in shape, and about 100 miles long.

Five generating stations supply the territory, three of which, Essex, Kearny, and Marion, are located in the Newark meadows area. Four stations, Essex, Kearny, Marion, and Burlington, are connected together by a 132-kv bulk power system; Perth Amboy, the smallest and least important station, feeds directly into the subtransmission system supplying central New Jersey. Three stations, Essex, Marion, and Burlington, also supply local subtransmission systems.

Other subtransmission areas are supplied by Hudson, Athenia, West Orange, Metuchen, Trenton, and Camden switching stations, which are connected to the bulk power system. In addition, the Roseland switching station connects the Public Service system to the Pennsylvania-New Jersey 220-kv system and the New Jersey Power and Light Company 110-kv system. Several other small interconnections are tied to the various subtransmission networks.

Figure 1 shows diagrammatically the location of the generating stations and switching stations, and the layout of the bulk supply and subtransmission lines.

All generators are normally hand-regulated and controlled under orders of a central load dispatcher. Several large synchronous condensers and frequency changers are located at switching stations and several small synchronous condensers and numerous small motor-generator sets which can be overexcited are located in substations. The larger synchronous condensers provide automatic regulation to some degree. Practically every four-kilovolt distribution circuit is provided with ± ten per cent or ± five per cent induction or tap-changing regulators and many circuits with low power-factor load are equipped with one or more banks of static capacitors. There are 708 four-kilovolt distribution circuits; 143 of these are entirely underground, 28 are more than 50 per cent underground and 87 are less than 50 per cent underground and the remaining 450 circuits are practically all overhead. There are 506 radial distribution circuits, 109 pure multiple-network circuits and 93 combination multiple-network and radial circuits supplying 90 separate networks.

Kilowatt Supply

In purchasing new units, it has been the practice to purchase turbines which have sufficient capacity to deliver full generator kilovolt-amperes output at 100 per cent power factor. Therefore, it has been possible to rerate the kilowatt output of the turbine-generators originally purchased on an 80 per cent or 85 per cent power-factor basis by

1. Increasing the power-factor and kilowatt rating of the generator and in some cases the power output of the turbine.
2. Taking advantage of any increased rating developed on field tests.
3. Using autotransformers to step up the voltage rating of the generator thereby gaining increased kilowatt, kilovolt-ampere, and power-factor ratings.

Table I shows the present turbine-generator capacities as of December 8, 1941, totaling 919,550 kw which is greater than the original kilowatt ratings by 20 per cent primarily due to increases in power-factor ratings. Work is now going forward on the installation of autotransformers on Kearny units 2 and 4 which will increase their kilowatt rating from 47,250 kw at 90 per cent power factor to 54,300 kw at 95 per cent power factor each.

Appreciating that these increased ratings approach the economy, thermal, and stability limitations of the machines, extensive studies and tests have been carried out to determine the values of these limitations. After an analysis of the various

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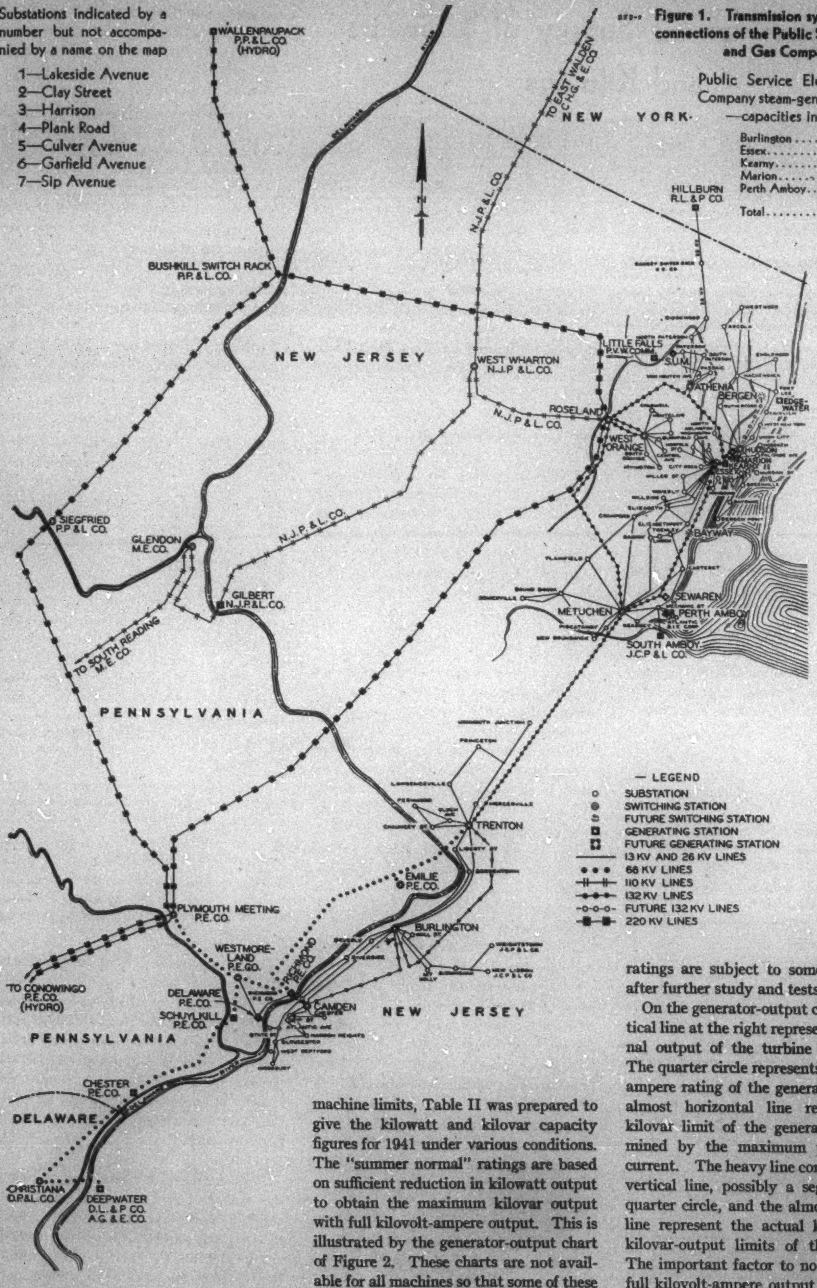
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FUTURE SWITCHING STATION
GENERATING STATION
FUTURE GENERATING STATION
13 KV AND 26 KV LINES
68 KV LINES
110 KV LINES
132 KV LINES
FUTURE 132 KV LINES
220 KV LINES

Figure 1. Transmission system and interconnections of the Public Service Electric and Gas Company

Substations indicated by a number but not accompanied by a name on the map

1—Lakeside Avenue
2—Clay Street
3—Harrison
4—Plank Road
5—Culver Avenue
6—Garfield Avenue
7—Sip Avenue

Public Service Electric and Gas Company steam-generating stations—capacities in kilowatts

Burlington	180,000
Essex	243,000
Kearny	325,500
Marion	150,800
Perth Amboy	20,250
Total	919,550

machine limits, Table II was prepared to give the kilowatt and kilovar capacity figures for 1941 under various conditions. The "summer normal" ratings are based on sufficient reduction in kilowatt output to obtain the maximum kilovar output with full kilovolt-ampere output. This is illustrated by the generator-output chart of Figure 2. These charts are not available for all machines so that some of these ratings are subject to some adjustment after further study and tests.

On the generator-output chart, the vertical line at the right represents the nominal output of the turbine in kilowatts. The quarter circle represents the kilovolt-ampere rating of the generator while the almost horizontal line represents the kilovar limit of the generator as determined by the maximum possible field current. The heavy line comprised of the vertical line, possibly a segment of the quarter circle, and the almost horizontal line represent the actual kilowatt- and kilovar-output limits of the generator. The important factor to note is that the full kilovolt-ampere output of the gener-

Table I. Turbine-Generator Capacities as of December 8, 1941

	Unit No.	Kva	Kw	Power Factor
Burlington	1	13,703	12,333	0.90
	2	13,703	12,333	0.90
	3	13,704	12,334	0.90
	4	22,500	18,000	0.80
	5	144,000	125,000	0.87
	5	207,610	180,000	
Essex	1	25,000	22,500	0.90
	2	25,000	22,500	0.90
	3	44,444	40,000	0.90
	4	40,000	36,000	0.90
	5	40,000	36,000	0.90
	6	40,000	36,000	0.90
	7	58,825	50,000	0.85
	7	273,269	243,000	
Kearny	1	44,444	40,000	0.90
	2	52,500	47,250	0.90
	3	44,444	40,000	0.90
	4	52,500	47,250	0.90
	5	44,444	40,000	0.90
	6	100,000	90,000	0.90
	1 mercury	25,000	21,000	0.84
	7	363,332	325,500	
Marion	1	12,632	12,000	0.95
	2	28,000	26,600	0.95
	4	10,000	9,000	0.90
	5–25 cycles	9,000	9,000	1.00
	6	9,000	8,100	0.90
	7–25 cycles	9,000	9,000	1.00
	8	9,000	8,100	0.90
	9	20,000	19,000	0.95
	10-high pressure	62,500	50,000	0.80
	9	169,132	150,800	
Perth Amboy	1	5,000	4,500	0.90
	2	5,000	4,500	0.90
	3	12,500	11,250	0.90
	3	22,500	20,250	
Total	31	1,035,843	919,550	
60 cycles	29	1,017,843	901,550	
25 cycles	2	18,000	18,000	
Steam	30	1,010,843	898,550	
Mercury	1	25,000	21,000	

Table II. Public Service System Kilowatt and Kilovar Capacities—1941

		Summer Normal		Winter 0.90 Power Factor Also, Summer Continuous Emergency		Winter 0.95 Power Factor Where Possible		Summer and Winter, Kilovars at 0 Kw
		Kw	Kilovars	Kw	Kilovars	Kw	Kilovars	Kilovars
Burlington	1					12,333	5,967	
	2					12,333	5,967	
	3					12,334	13,500	
	4					18,000	13,500	
	1-2-3-4	50,000	33,000	55,000	31,400	55,000	31,400	38,000
	5	125,000	70,000	125,000	70,000	125,000	70,000	94,000
	Station	175,000	103,000	180,000	101,400	180,000	101,400	132,000
Essex	1	20,000	15,000	22,500	10,900	23,750	7,800	16,000
	2	20,000	15,000	22,500	10,900	23,750	7,800	16,000
	3	38,000	23,000	40,000	19,400	40,000	19,400	26,000
	4	33,000	22,000	36,000	17,400	37,500 (*A*)	13,900	26,000
	5	33,000	22,000	36,000	17,400	37,500 (*A*)	13,900	26,000
	6	33,000	22,000	36,000	17,400	37,500 (*A*)	13,900	26,000
	7	50,000	31,000	50,000	31,000	55,000	19,600	37,000
	Station	227,000	150,000	243,000	124,400	255,000	96,300	173,000
Kearny	1	36,000	26,000	40,000	19,400	40,000	19,400	31,000
	2	45,000	27,000	47,250	23,000	49,900	16,400	34,000
	3	36,000	26,000	40,000	19,400	40,000	19,400	31,000
	4	45,000	27,000	47,250	23,000	49,900	16,400	34,000
	5	36,000	26,000	40,000	19,400	40,000	19,400	31,000
	6	90,000	44,000	90,000	44,000	90,000	44,000	52,000
	1 mercury	20,000	15,000	21,000	13,300	21,000	13,300	20,000
	Station	308,000	191,000	325,500	161,500	330,800	148,300	233,000
Marion	1	10,000	8,000	12,000	4,000	12,000	4,000	9,000
	2	22,500	12,000	26,600	9,000	26,600	9,000	15,000
	4	9,000	4,400	9,000	4,400	9,500	3,120	6,000
	5–25 cycles	9,000	0	9,000	0	9,000	0	0
	6	8,100	3,900	8,100	3,900	8,550	2,800	6,000
	7–25 cycles	9,000	0	9,000	0	9,000	0	0
	8	8,100	3,900	8,100	3,900	8,550	2,800	6,000
	9	17,000	10,000	19,000	6,000	19,000	6,240	12,000
	10-high pressure	45,000	43,000	50,000	37,500	59,300 (*B*)	19,500	47,000
	Station	137,700	85,200	150,800	68,700	161,500	47,460	101,000
Perth Amboy	1			4,500	2,180			
	2			4,500	2,180			
	3			11,250	5,450			
	Station	18,000	13,000	20,250	9,810	20,250	9,810	16,000
Total all stations		865,700	542,200	919,550	465,810	947,550	403,270	655,000
Firm reactive capacity			260,900		260,900		260,900	262,900
System total		865,700	803,100	919,550	726,710	947,550	664,170	917,900

A—Essex 4, 5, 6 can deliver 37,500 kw each only if Essex 7 is in operation. With the low-pressure boilers only, there is insufficient steam-pipe capacity to these units.

B—Depends on ability of low-pressure units to absorb steam from high-pressure unit 10.

ator is not available at all power factors; Public Service system machines have ratings as synchronous condensers of only about 65 per cent of their kilovolt-ampere rating.

Since stability ratings are relative values, the criterion assumed for the transient-stability studies of individual machines was that the phase-angle displacement for a selected rating should not exceed 50 per cent of the pull-out angle in the time required to clear a fault at the machine terminals. The studies show that the phase-angle displacement is much more dependent on the turbine power output than on the generator power factor. Therefore, generator power-factor limits have been raised to 95 per cent which are shown in Table II.

The kilowatt ratings given are within all limitations on all condensing turbine-generators. In the cases of the noncondensing units (that is, the superposed machines) the ratings given are within all limitations except the stability limit for faults on the 13,200-volt bus to which the particular machine is connected. Therefore, during lightning or sleet storms when system trouble is frequently experienced, it may become desirable to favor the loading of these units by shifting the load from them to less efficient units normally operating in reserve.

So far consideration has been given only to the limitations in the electrical plant, but the use of superposed units and large high-pressure boilers introduces the factor of boiler capacity into the kilowatt supply problem. Low-pressure boilers connected to a main header with one spare boiler installed for a group of ten or so were desirable and economical. However, with high-pressure high-capital-cost boilers which should require cleaning only once in six months, the economy of a spare boiler is questionable. Therefore, a large high-pressure boiler outage generally makes unavailable some high-pressure turbine capacity due to this lack of flexibility in the boiler plant. The tables shown in this paper do not make allowances for both boiler and generator outages in different stations. This problem requires further study and a definite conclusion regarding the relations of the boiler plant to system capacity.

Kilovar Supply

Table III shows the present-system reactive capacity other than in generators as of December 1, 1941. Some of this capacity is considered as either a part of the load or nonfirm; 260,900 kilovars is firm capacity, which is considered the

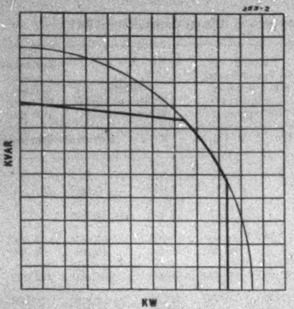

Figure 2. Generator output chart showing typical relative kilowatt and kilovar capacity

same as generator capacity. This has been installed largely to correct power factor and relieve overloaded or low-voltage conditions as they have developed individually. Recently, as the static-capacitor program developed, the broader picture of kilovar supply has been studied so as to co-ordinate the entire program. Prompted by the desire to obtain increased kilowatt generator ratings to meet the recent greatly increased industrial loads, the past conception of carrying the major part of the kilovar load on the generators has had to be abandoned in favor of kilovar capacity installed at the load. The problem has therefore developed into the manner in which this could be accomplished with the greatest speed and lowest cost.

Kilovar capacity can be provided by the use of

Extra copper in the generator stator and field windings

Synchronous condensers

Oversize synchronous motors on motor-generator sets

Static capacitors

Reactive capacity on customers' premises

The use of reactive capacity in one form or another and in appropriate locations, can accomplish the following results in addition to the obvious one of carrying reactive load:

Control voltage (raise only, with static capacitor)

Reduce kilvolt-ampere load between the capacity and the generator

Reduce system investment

Reduce system losses due to kilowatts and kilovars

An analysis has been made of the system costs to determine those elements which are affected by low power factor, starting with the generator as a base and extending to the first load point of the distribution circuit. In the generating station, only the cost of the generator and its switching equipment has been assumed to vary. In switching stations and substations, only transformers and switching equipment have been considered, omitting land, building, masonry compartments, and switchboard. In bulk supply lines, rights of way have been omitted, and in subtransmission and distribution circuits rights of way, poles, and conduits have been neglected. On this basis the incremental investment per kilovolt-ampere which varies with the load power factor has been determined.

The relative net cost of installing kilovar capacity in generators, large synchronous condensers in switching stations, small synchronous condensers in substations and static capacitors on distribution circuits has been determined, evaluating the cost of the equivalent kilovar capacity, the added system investment component effected by kilovar loading, and the incremental losses. These computations show that

1. The static-capacitor installations are the most economical because they are close to the load.

2. The small added cost of providing reactive capacity in generators is next least expensive even with the added system investment and no saving in losses.

3. Adding capacity in the form of synchronous condensers wherever located is the most expensive of all due to their higher initial cost.

Of course, these conclusions apply only to the Public Service system or to those cases where the breakdown of system costs is nearly the same. Other situations would have to be analyzed similarly to arrive at a proper answer.

The limit on the amount of static capacitors which can be installed without switching depends upon the kilovar load at light-load periods, the permissible generator power factor, and the limitations on the distribution system. To determine the maximum amount of static capacitors which can be installed, the annual kilovar load duration curve for the Public Service system was estimated for 1942 based on past records and is shown in Figure 3. This figure shows a minimum net 250,000-kilovar load including the effects of the inherent reactive capacity in the system; however, an investigation of the possible locations showed that the capacitor installations should be limited to 150,000 kilovars effective at their location. At peak loads this would have the effect of reducing the kilovar load on generators by nearly 200,000 kilovars. It is felt that further experience may show that the distribution plant can absorb more of this capacity without encountering the problem of picking up the system after a major shutdown, and also without experiencing a self-excitation problem on lightly-loaded generators. After the limit of static capacitors is reached, if additional reactive capacity cannot be provided economically in new generators or maintained in existing generators, because of the value of their kilowatt output, synchronous condensers offer a quick and economical way in which kilowatt generation can be obtained to meet unexpected demands. The higher over-all cost of large synchronous condensers may be partly offset by the convenience of their operation and their stabilizing effects.

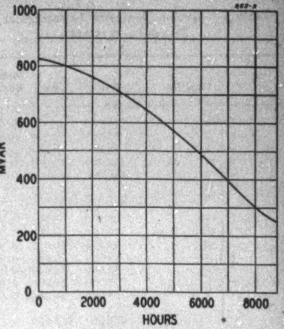

Figure 3. Annual megavar load-duration curve for 1942

Static Capacitors

Provision of reactive capacity for system purposes in the distribution plant, in the form of static (shunt) capacitors, in addition to improving system conditions with respect to increased kilowatt and kilovar capacities and reduced system losses, affords marked advantages in the distribution plant itself in the form of postponement of facilities, improvement of voltages, and decrease of losses. These features of the shunt capacitor have been so well-publicized that extended comment appears unnecessary. It should be pointed out, however, that when system requirements dictate such installation of shunt capacitors, the location within the associated part of the distribution system may be so selected that optimum concurrent distribution benefits are achieved. Since the major justification of such installation lies with the system requirements, some expansion of the natural field

Table III. System Reactive Capacity (Other Than in Generators)

	Kw	Kvars	
Considered as Firm Capacity			
Synchronous condensers			
Athenia switching station		40,000	
Roseland switching station		30,000	
Trenton switching station		15,000	
Garfield substation		5,000	
Gloucester substation		4,000	
Hoboken substation		5,000	
Olden Avenue substation		5,000	
			104,000
Railway motor-generator sets with oversized motors			
Norfolk Street substation		5,000	
Plainfield substation		4,900	
			9,900
Frequency changers			
Marion generating station	0	32,000	
	16,000	30,000	
	18,000	28,000	30,000 average
Static capacitors on distribution lines		99,000	
			99,000
220-kv transmission system			
Public Service share of capacitance		18,000	
			18,000
Total firm reactive capacity			260,900*
Not Considered as Firm Capacity			
Frequency changer			
Metuchen switching station (available at times on application to Pennsylvania Railroad and Philadelphia Electric companies			20,000
Total nonfirm reactive capacity			20,000
Reactive Capacity Considered as Part of the Load			
25 railway motor-generator sets		29,000	
			29,000
Bulk transmission system			
132-kv cable 9 miles		35,000	
132-kv open wire miles		22,900	
			57,900
Subtransmission system			
13/26-kv cable 554 miles		27,500	
13/26-kv open wire 638 miles		1,500	
			29,000
Total Reactive Capacity in Load			115,900

*Plus or minus 2,000 depending on load on Marion frequency changer.

Table IV. Comparison of 1942 Summer Loads and 1941 Summer Normal Capacity

	All in		One out		Two out	
	Megawatts	Megavars	Megawatts	Megavars	Megawatts	Megavars
Load	818	825				
Less trolley bus	− 11	0				
Net load	807	825	807	825	807	825
Installed capacity	866	803	866	803	866	803
Loss of first unit			−125	− 70	−125	− 70
Loss of second unit					− 90	− 44
Interconnection	0	0	0	0	0	0
Unavailable reserves	0	− 37	0	− 37	0	− 37
Net Public Service capacity	866	766	741	696	651	652
Total Public Service reserve and excess Public Service capacity	59	− 59	− 66	−129	−156	−173
Authorized 1942 program		150		150		150
After completion of program	59	91	− 66	21	−156	− 23

of the shunt capacitor is warranted in the realization of minor distribution economies. Under present long-delivery schedules for equipment, the capacitor may well be employed as a temporary expedient for overload conditions without extreme penalty, because of its low installation cost and high mobility. Furthermore, long-range economy of the capacitor versus other forms of plant addition may now indicate the capacitor as the proper solution because of the temporary nature of certain loads now being taken on and the high salvage value which is realized in the capacitor.

The experience gained from some 125,000 kva-years of shunt capacitor operation may be of interest to those who are as yet undecided as to the desirable characteristics of capacitors. On the Public Service system bulk capacitors are installed in 180-kva, three-phase banks, using 12 individual 15-kva units for each installation. The banks are wye-connected, with mid-point solidly connected to circuit neutral. Indicating-type cutouts are used on phase connections, phase and neutral lightning arresters are usually employed, and capacitor cases are grounded.

No case of harmonic resonance has as yet been encountered with shunt capacitors. Some instances of telephone inductive interference on common-neutral circuits equipped with capacitors have required use of neutral reactor on the capacitor bank; but the cost of such corrective equipment is not regarded as sufficient to prove out the capacitor for its peculiar purposes.

The operating record of all capacitor units on the Public Service system is regarded as highly satisfactory. Of a total of some 5,600 15-kva units, 25 units have been removed from the lines for various reasons. Of this number, 17 units failed electrically after service periods of from one month to two years. The remaining eight units developed mechanical difficulties, such as bulged tank, leaking bushing, or open tank seam, requiring removal of the unit for repairs.

Improvements in System Protection

Recognizing that markedly increasing the kilowatt rating of generators has decreased their original margin of stability, it has become necessary to adopt several measures for improving the system protection. Many stability studies have shown that the best means of increasing the stability of a system, aside from eliminating faults or restricting them to a single phase, is rapid fault removal. Reference has already been made to the slight improvement in stability with higher generator field strengths or lower power-factor operation of high-speed low-inertia superposed generator units. A careful review of the system has been made which has produced the following recommendations:

1. Rebuilding of overstressed circuit breakers for higher speeds and greater capacities.
2. Reinsulating of certain station busses and lines with post-type insulators.
3. Reconstruction of a major part of the open-wire subtransmission system to a protected phase design.
4. Installation of automatic-generator field control to insure the proper maintenance of adequate field strengths at all times.
5. Installation of high-speed carrier and pilot-wire relays in the bulk supply lines and more important subtransmission lines.
6. Installation of voltage-controlled system-segregating relays to act as backup protection on prolonged system disturbances.
7. Splitting of 132-kv system into separate or higher impedance systems by operating with sectionalized busses at certain locations.
8. Continuance of bus differential protection on all principal busses under all conditions of operation.

Tap-Changing Equipment

Following the normal practice of hand field regulation on generators and carrying as much of the kilovar load on the generators as is possible or economical may lead to excessive voltage gradients in the transmission and distribution system. Also faced with the necessity of maintaining certain voltage limits on switching station, substation, and high-voltage customer busses and holding within a ±3 per cent voltage variation on regulated primary and secondary customers' services, it becomes necessary to install either synchronous condensers in substations or tap-changing-under-load equipment on certain step-up and step-down transformers even though the generator busses are regulated higher during heavy-load periods and lower at light loads. In some cases tap-changing equipment is cheaper, if sufficient kilovar capacity is available, and the kilovar load can be transmitted without incurring too much loss.

In fact, tap-changing equipment is necessary to some extent to avoid building up excess reserves of kilovar capacity. Network-analyzer load-flow studies have demonstrated that unavailable kilovar capacity may be as much as ten per cent of the installed capacity, and the installation of tap changers in transformers between generating stations can frequently make much of this capacity available and still meet given bus-voltage limitations. Therefore this type of equipment may avoid the installation of kilovar capacity by keeping the unavailable kilovar capacity to a minimum and maintaining customers' service voltages within given limits when sufficient kilovar capacity is available in generators or condensers at other locations. It is, therefore, possible to install kilovar capacity at the most advantageous location which will release other capacity for the benefit of other locations. The installation of a ±5 per cent tap-changing transformer at Essex generating station will release approximately 25,000 kilovars of excess station capacity to the system and at the same time will improve the 132-kv system voltage conditions.

Analysis of Loads and Comparison With Capacity

There must be sufficient power and reactive capacity (kilowatts and kilovars) in service to handle the system loads under all reasonable conditions. It is already obvious that the power capacity is provided by turbine generators. In the Public Service system there is a slight amount available in interconnection diversity entitlements and load curtailment obtainable by operating all-service busses on their gasoline engines. The maximum power capacity is a relatively definite figure, being fixed by the outputs of the various turbines as shown in Table II.

The reactive capacity consists of the excess capacity in generator stator and field windings and the capacities of synchronous condensers and motors and static capacitors, plus a proportionate share of the capacitance in the 220-kv system. The maximum reactive capacity is a relatively indefinite figure since the excess capacity in generators is highly variable depending on the kilowatt load carried as shown in Figure 2. According to the power output of the generators and their power factors, their reactive outputs may vary through an overall range of 0 per cent up to 65 per cent of their kilovolt-ampere ratings.

As with kilowatts the installed kilovar capacity on the system must be greater than the arithmetic sum of the kilovar loads and the assumed reserves except that

1. Generator kilowatt outputs and bus-voltage limitations make it impossible to distribute the kilovar load among the sources exactly.
2. When kilowatts are being taken from an interconnection during an emergency, it must be possible to supply kilovars to that interconnection in order to maintain a reasonable voltage.

Therefore, a greater percentage of kilovar reserves is required than for kilowatt reserves.

Kilowatt and kilovar loads are determined by the reading and summation of all sources of this capacity considering the losses in the transmission and distribution system as a part of the load. The kilovar losses in the system are partially offset by the inherent reactive capacity in the system considered as a part of the load. Kilowatt and kilovar quantities are metered directly because power-factor meters and kilovolt-ampere readings do not permit a sufficiently accurate determination of these quantities. System load data for Public Service Company are estimated for 1941 and 1942 as shown in Table A.

Table A

Condition	1941		1942	
	Megawatts	Megavars	Megawatts	Megavars
Annual peak, December (5–6 p.m. hourly integrated)	878	627	965	692
Winter day	742	709	817	777
Summer day	734	739	818	825

Every additional kilowatt of industrial load (day load) has associated with it approximately one kilovar of reactive load; this reactive load must be carried just as surely as the power load. Therefore, every kilowatt of turbine capacity added to the system must have added with it one kilovar of reactive capacity for industrial load and about one-half kilovar for residential load. Since reactive loads cannot be economically transmitted as far as power loads, this means that reactive capacity needs to be selected and located more carefully than power capacity, generally closer to the load.

It is evident from the system load data that the lower day load power factor requires a portion of the kilowatt capacity over the December peak to handle the succeeding heavy summer kilovar load. Also the December peak kilowatt load does not greatly exceed the following summer kilowatt load. Therefore in any given calendar year when the load is steadily growing the required installed capacity is determined when maintenance schedules are the heaviest and failures and system disturbances are most likely to occur, that is during the summer period.

Consideration has been given to kilowatt- and kilovar-load reduction by reducing system voltage, but since system- and customer-voltage regulation is obtained by feeder regulators installed in substations, the system voltage reduction would not take effect until the regulators had locked out. Since system stability is also rapidly decreased with system voltage, and tests have shown that a reduction in voltage reduces the load almost proportionately, this scheme of load reduction is not considered feasible or desirable. Hence the problem reduces to one of providing adequate facilities. If, for any reason these facilities are not sufficient, load must be dropped or customers must be called upon for load reduction.

A comparison of 1942 summer load and summer normal capacity is shown in Table IV for the Public Service system with one and two units out of service. Based on such a study, the 1942 program for 150,000 kilovars of additional reactive capacity was authorized. This is made up of 50,000 kva of static capacitors installed on distribution circuits which reaches the established limit of 150,000 kilovars of capacitor installations and 77,500 kva of synchronous condensers in 7,500-kva and 5,000-kva units installed on four-kilovolt busses in substations. This 127,500 kva of installed capacity together with the calculated reduction in system losses makes up the 150,000 kilovars shown in the tabulation. Some of this capacity may be used to permit higher power-factor operation of the generators during the summer. Maintenance schedules for 1942 have been arranged so that no more than one unit is expected to be unavailable for any cause during the summer, and the spinning reserves on the Pennsylvania-New Jersey interconnection are sufficient to meet all requirements.

A similar tabulation for 1943 shows that an additional 30,000 kilovars of capacity is required, and this is to be provided by the installation of a 30,000-kva outdoor hydrogen-cooled synchronous condenser at Roseland switching station. This will continue the maintenance of generator kilowatt ratings for high industrial loads.

Conclusions

It is not the intention of the authors to summarize or restate the many conclusions which have been tabulated throughout the article but to close their case by focusing attention on three things:

1. The data which form the basis of the studies apply only to the Public Service system and should not form the basis of a generalization but may be accepted as a method of analysis.

2. Reactive capacity in one form or another, particularly static capacitors, can quickly make available additional kilowatt capacity and, by proper location, postpone the installation of additional transmission and distribution facilities.

3. The conception of a separate kilowatt and kilovar load and supply should offer a practical means of presenting to administrative officers the highly technical problem of providing adequate electrical-supply facilities.

It is hoped that a method of analyzing facilities for the supply of kilowatt and kilovar loads has been presented so that utility engineers may at this time of shortages in materials approach the problem objectively with the intention of getting the utmost capacity out of existing equipment without decreasing the services which are so urgently needed by industry and the nation.

A Study of the Modified Kramer or Asynchronous–Synchronous Cascade Variable-Speed Drive

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Synopsis: Several large wind-tunnel drives recently built have involved a system of speed control which has seldom been used commercially, and a number of new problems had to be solved. The system consists of a wound-rotor induction motor whose slip rings are connected to a synchronous motor driving a variable-speed d-c generator which feeds a constant-speed d-c–a-c set putting the major part of the secondary power back into the line.

This drive is found to be very efficient and particularly suited to very large fan or pump drives where a wide range of speed is required. The problems of steady-state and dynamic stability are discussed and some novel methods of analysis given.

LARGE wind tunnels require a wide range of speed and accurate speed control and if their use factor is high, a high efficiency over the working range is desirable. Also minimum disturbance to the power system is important in many cases. To meet these requirements a speed-control system has been adopted which, while not new in principle, involved the solution of a number of interesting problems. The system as shown in Figure 1 consists of a wound-rotor driving motor (A) whose secondary winding feeds a synchronous motor (S_1) driving a variable-speed d-c generator (DC_1) which in turn drives a d-c–a-c set (DC_2 and S_2) to return most of the secondary power back to the line. The name "modified Kramer set" is suggested by the authors since the scheme involves conversion of the secondary power to d-c and field control for the speed changes as in the well-known Kramer set. The term "modified" was used because the Kramer set used a rotary converter and the d-c power was usually fed into a d-c motor on the same shaft as the main motor. Another descriptive name would be asynchronous-synchronous cascade.

Speed Control and Design Features

Speed control is accomplished in this system by controlling the speed of the variable-speed set, and as long as the synchronous machine remains in synchronism with the induced secondary voltage, the motor must run at a speed corresponding to the difference in frequencies. The speed of the variable-speed set can be adjusted by changing field of either the d-c motor or the d-c generator, just as in a wide-range variable-voltage d-c system. The inherent speed regulation is determined by the regulation of the d-c machines. The application of an accurate speed regulator to such a system is a subject in itself, too lengthy for this paper. It is apparent that automatic control of the speed can be had by controlling the d-c fields.

The constant-speed set is started first since it is smaller and requires relatively little starting current. The variable-speed set may then be brought to speed and the synchronous motor field energized. In this manner it is possible to excite the main motor from the secondary while it is stationary and disconnected from the line. After careful adjustment of frequency and voltage, the primary winding may be synchronized just as an oncoming generator may be. This control also can be made fully automatic.

In order to understand the design problem involved, one must consider the power distribution as shown in Figure 2 for a typical fan curve. It will be seen that the maximum secondary power to be handled occurs at two thirds of synchronous speed, but the maximum torque on the variable-speed set will be at full speed. Hence it is desirable to work at full flux throughout the upper range of speed, obtaining speed control by changing the field of the constant-speed d-c motor. However, in order to limit the size of this d-c motor it is found desirable to obtain the lower part of the speed range by field control of the generator holding nearly constant d-c voltage. The exact point of changing from control of motor to generator field is determined by the economics of the design.

Steady-State Stability

Some interesting problems arise in connection with the stability of such a combination, and the methods developed for handling these may be of value in other similar problems.

Since the synchronous motor must stay in synchronism, the machines must be designed so that their combined characteris-

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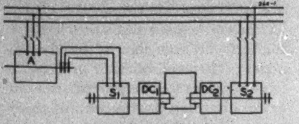

Figure 1. Schematic diagram of drive and auxiliary machines

Asynchronous–synchronous cascade

tics result in stability. For the steady-state synchronizing-torque calculation, the induction motor may be treated approximately as a transformer with high magnetizing current just as in the conventional induction-motor theory. However, the secondary circuit now includes the impedances and the internal voltage of the synchronous machine. Hence the stability calculation resolves itself into a simple two-machine problem.

Another exact method was given in a previous paper on the doubly fed machine.[1] This consisted in resolving the torque into components:

$$T=\left[e_1^2 r_2 s-e_2^2 r_1+e_1 e_2\sqrt{l_1^2+m_1^2}\times \sin(\delta+\alpha)\right]\frac{1}{l^2+m^2}$$

where l_1, m_1, l and m are functions of the slip s. The first component is the well-known induction-motor torque (but, of course, including the total secondary-circuit impedance). The second component is due to the primary resistance and secondary excitation and is negative. The third component is a function of the angle $(\delta+\alpha)$ and when smaller than 90 degrees results in positive torque increasing with the angle.

The components of the stator current of the induction motor are due to primary voltage and the synchronous-machine excitation.

$$I_{11}=E_{L1}\frac{f+jh}{l+jm}$$

$$I_{12}=-E_{L2}\frac{\cos\delta-j\sin\delta}{l+jm}$$

Thus the angle between I_{11} and $-I_{12}$ will be

$$\beta=\delta-\tan^{-1}\frac{h}{f}$$

The primary current I_1 is determined by the output and power factor desired at a given speed. For the same speed (slip) the current component I_{11} can be easily found by solving the equivalent circuit for $E_{L2}=0$ or by using adequate formulas or from the circle diagram of the induction machine for $E_{L2}=0$. Thus the current component I_{12} can be found as the geometrical difference between I_1 and I_{11}. In Figure 3, the primary current I_1 is fixed. The current component I_1 is found from the circle diagram A as the current corresponding to the slip on which I_1 is based. Assuming constant slip and variable angle δ between E_{L1} and E_{L2} the vector I_{11} will not change its position, the vector I_{12} will describe a circle (B in Figure 3) with the end point of I_{11} as center and I_{12} as radius. This circle is the circle diagram of the primary current I_1 for constant slip and variable angle δ. From this it is possible to judge the approximate overload capacity of the machine for the conditions given by the current I_1.

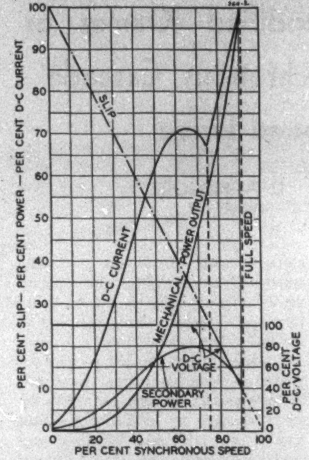

Figure 2. Characteristic curves

Dynamic Stability

The load, the driving motor, and the connected sets constitute a system of masses connected by springs or by electrical ties which act like spring connections. These masses can oscillate at a number of different natural frequencies corresponding to different modes of vibration. These modes of vibration could theoretically be excited by pulsating torques or could be set in sustained oscillation if there was sufficient energy fed into the oscillating system by the negative damping characteristics of some machine.

Before analyzing the rest of the system it is well to consider the nature of negative damping in such machines. A method of analysis for negative damping in both the doubly fed machine and the synchronous machines has been discussed in previous papers[1,2] and is being discussed in another current paper.[3]

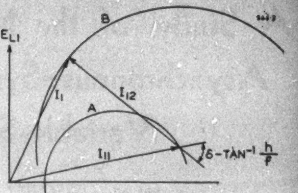

Figure 3. Circle diagram of the doubly fed machine

However, it is possible to get a very clear picture of the physical problems and fair quantitative results for large machines by making some simplifying assumptions. These are:

The per-unit primary resistance of the large induction motor is small.

The rotor impedances of the synchronous motor at the oscillation frequency are equal on the two axes.

This gives a revolving field of flux linking the stator winding of the induction motor which is determined only by the fixed primary voltage and frequency. Any small angular oscillations of the rotor induce additional voltages in the secondary at slip frequency plus the oscillation frequency and slip frequency minus oscillation frequency. These voltages are superimposed on the steady-state quantities and act through the reactance of the induction machine (as viewed from the secondary) and the equivalent impedances of the synchronous machine. Both of these extra components induce oscillation frequency currents in the rotor of the synchronous machine, and its impedance must be evaluated on this basis. This would form a basis for calculation as indicated in appendix A, in which the damping coefficient is determined from the speed-torque curve of the induction motor with its rotor short circuited through the impedance of the synchronous machine. The negative-damping coefficient (ratio of change in torque to change in velocity) in per unit is shown to be the slope of a line drawn between two points at $(s+\Delta)$ and $(s-\Delta)$ slip values. To further simplify the approximate solution, let us assume that the resistance of the rotor circuit of the synchronous machine is negligible while considering the effects of primary resistance, and that the primary resistance is negligible while considering the effects of secondary resistance. This is approximately true because the reactance predominates in determining the currents and amplitudes, so that the losses and in-phase components of currents may be approximated by calculating the effect of the resistances one at a time.

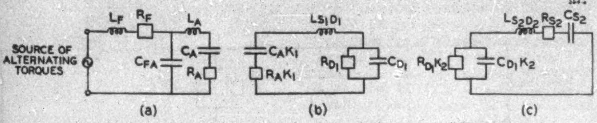

Figure 4. Electrical analogy for the parts of a mechanical system

This results in the approximate solution that the resistance of the secondary circuit acts to cause a negative damping whose value depends on the slope of the speed-torque curve of the induction motor short-circuited through the impedance of the synchronous machine, neglecting rotor resistances. Again since the actual rotor currents involved are at slip frequency plus and minus the oscillation frequency, this slope is to be determined between two points at slip values $(s+\Delta)$ and $(s-\Delta)$. This can readily be seen to be true for the case where the oscillation frequency becomes very low, and one may think of the oscillation as moving from point to point on the speed-torque curve.

The effect of the resistance of the synchronous-motor rotor circuits is to produce positive damping of any angular oscillations between the machines. Again the same concept of taking the slope of the speed-torque curve may be used, except that the slip is zero. One can make an approximate allowance for the difference on the two axes by taking the direct-axis equivalent circuit and a voltage proportional to the flux in the quadrature axis, and a voltage for the quadrature-axis flux proportional to the direct-axis flux. This relation is due to the fact that it is the flux on one axis that induces voltage in the rotor circuit of the other axis due to small angular oscillations.

Electrical Equivalent of Mechanical Systems

According to well-known methods one can analyze torsional vibration problems by representing the parts by their electrical equivalents, as shown in Table I. Thus the equivalent circuit for the fan load and the induction machines will be represented by Figure 4A. L_F and R_F correspond to the moment of inertia and the damping of the fan; C_{FA} represents the spring of the shaft between the fan and the rotor of the induction machine; L_A corresponds to the moment of inertia of the rotor of the induction machine. C_A and R_A correspond to the change of power of the induction machine due to the synchronizing torque, and the damping between the synchronous machine (S_1) and the induction machine. The resistance R_A can be negative for negative damping.

Table I

Mechanical Quantities	Electrical Equivalent
Angular velocity	Current (I)
Torque	Voltage (E)
Moment of inertia	Inductance (L)
Torsional flexibility (reciprocal of stiffness)	Capacity (C)
Damping (loss proportional to oscillation velocity squared)	Resistance (R)

The moments of inertia (L) will be expressed in pound-feet-seconds² per mechanical radian, the synchronizing torques and torsional stiffness ($1/C$) in pound-feet per mechanical radian, and the damping torques (R) in pound-feet-seconds per mechanical radian.

Since the synchronous machine (S_1) has a different number of poles and a different angular velocity (speed) than the induction machine, the capacitance and the resistance that correspond to its synchronizing torque and its damping will have other values than those of the induction machine (C_A and R_A) namely

$$C_{S1}=C_A\left(\frac{P_{S1}}{P_A}\right)^2=C_AK_1$$

$$R_{S1}=R_A\left(\frac{P_{S1}}{P_A}\right)^2=R_AK_1$$

This is in accordance with the fact that the power which corresponds to the synchronizing torque and the damping is the same for both machines.

Any oscillations of the synchronous machine (S_1) will be transmitted to the DC_1 and an alternating electromotive force will be induced in the armature of this machine of the value

$$e=\frac{N}{2\pi}\frac{P}{a}\phi 10^{-8}\frac{dw}{dt}\text{ volts}$$

This electromotive force will produce a current that depends on the ohmic resistances and the inductances of the armature circuits of both d-c machines, and, as a consequence, power will be transmitted from the DC_1 to the DC_2. This power transmission will damp the oscillations, that is, it will act as a damping force. The inductance causes a lagging of the current and the resultant torque. This gives a component of torque 180 degrees out of phase with the displacement, hence it is analogous to a spring. Thus the d-c machines are connected by spring and damping torques. The corresponding capacitance and resistance can be assumed connected in parallel or in series. The paralleling has the advantage that the formulas obtained are more general, independent of the frequency of the oscillation.

For this case the damping torque of the DC_1 will be

$$I=\frac{e}{\Sigma R}\qquad T_d=IE_L\frac{0.739}{\Omega}\text{ lb ft sec}$$

and the synchronizing torque

$$T_s=\frac{\Sigma R}{\Sigma L}T_d\text{ lb ft}$$

E_L is the armature voltage of the d-c machine, ΣR and ΣL are the sums of the resistances and inductances of the circuit of both d-c machines, Ω is the angular velocity of the DC_1.

The equivalent circuit for the synchronous machine (S_1) and the d-c machine (DC_1) is shown in Figure 4B. L_{S1D1} represents the moment of inertia of the variable-speed set (S_1+DC_1), R_{D1} represents the damping and C_{D_1} the synchronizing torque of the DC_1.

Both the d-c machines are coupled by spring and damping in the same manner as the synchronous machines (S_1) and the induction machine (A). For similar reasons the constants R_{D2} and C_{D2} that represent the DC_2 are different from R_{D_1} and C_{D1}: both d-c machines have different speed and different coil fluxes. The ratio of the torques produced by the current I in both machines is

$$\frac{T_{D2}}{T_{D1}}=\frac{n_{D1}}{n_{D2}}\frac{(N\phi)_{D2}}{(N\phi)_{D1}}$$

Thus the constants for the DC_2 will be

$$K_{D2}=R_{D1}K_2\qquad C_{D2}=C_{D1}K_2$$

$$K_2=\frac{n_{D1}}{n_{D2}}\frac{(N\phi)_{D2}}{(N\phi)_{D1}}$$

The equivalent circuit for the constant-speed set (DC_2+S_2) will be therefore represented by the Figure 4C. $L_{S_2D_2}$ represents the moment of inertia of the constant-speed set (DC_2+S_2), R_{S_2} represents the damping, C_{S_2} the synchronizing torque of the synchronous machine (S_2).

Using the equivalent circuit Figure 4 it is possible to set up the differential equation for the whole set. By solving this equation the resonant frequencies can be found, and the damping factors of the oscillations can be determined. However, it is more convenient to connect the three separate circuits of Figure 4 to one circuit and to set up the differential equation for this circuit. Dividing all constants of

Figure 4B by the factor $(P_{S1}/P_A)^2$ and all constants of Figure 4C by the factor $(P_{S1}/P_A)^2 \times K_2$ the equivalent circuit Figure 5 will be obtained.

SOLUTION OF EQUIVALENT CIRCUITS FOR THE ELECTRICAL ANALOGY

An electrical system such as shown in Figure 5 can readily be solved for a given frequency, however, the determination of the natural frequencies is quite difficult. In the practical problem described in reference 4 a set of simultaneous equations was set up for the branches of the network, and a numerical solution obtained by matrix methods. This involves four complex roots or eight distinct roots and is too laborious for general use. A more practical semigraphical solution with the usual complex number representation of the impedances was obtained by plotting the current for a given applied voltage at different frequencies. Two frequencies between which the phase of the current reversed were taken as an indication that resonance lay between them, and more points were tried near the peak currents to determine the exact resonance. The circuit was set up approximately on the a-c calculating board. Although negative resistance or even the very low positive resistance of some branches could not be represented, the solution did indicate the points of resonance which could then be calculated accurately.

The circuits were checked at the natural frequencies for any net negative damping assuming a small impressed force in the branch containing the negative resistance. Also, the amplitudes for any impressed forces at the propeller at the natural frequencies were checked by these methods.

Conclusion

Methods have been indicated for determining the stability limits of this system, and the dynamic stability or hunting characteristics have been analyzed. The results of the application of this type of analysis indicate that the machines can readily be designed to be stable. Also providing the motor is operated well below synchronous speed, 8 or 12 per cent below for large machines, it is possible to avoid any tendency to hunt.

List of Symbols

This does not include certain symbols defined as used, or the symbols indicating the machines as shown in Figure 1.

a—Number of parallels

e_1, e_2—Impressed voltage of primary and secondary

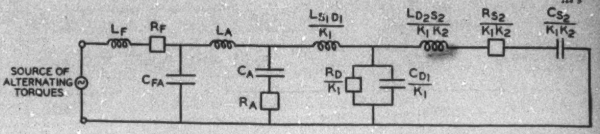

Figure 5. Electrical analogy for the combined mechanical system

E_{L1}, E_{L2}—Induced voltages in primary and secondary

$f=(1+\tau_2)$

f_1, f_2—Frequency of primary and secondary impressed voltage

$h=-r_2/sx_m$

I—Current

I_m—Magnetizing current

$l=[(1+\tau_2)r_1+(1+\tau_1)r_2/s]xs$

l, L—Inductance

$m=\left[x_1+(1+\tau_1)x_2-\frac{r_2}{\tau_1}\frac{r_1}{x_m}\right]xs$

n—Rpm

N—Total number of conductors

r_1, r_2—Stator and rotor resistances

r_{Dd}—Resistance of damper winding on the direct axis

r_{Dq}—Resistance of damper winding on the quadrature axis

r_f—Field resistance

s—Per-unit slip

T—Torque

T_d—Damping torque per radian per second

T_s—Synchronizing torque per radian

x_1, x_2—Stator and rotor leakage reactance

x_{ad}—Reactance of armature reaction on direct axis

x_{aq}—Reactance of armature reaction on quadrature axis

x_{Dd}—Leakage reactance of direct-axis damper winding

x_{Dq}—Leakage reactance of quadrature-axis damper winding

x_f—Leakage reactance of the field

x_m—Magnetizing reactance

Z—Impedance

α—Power-factor angle

β—Angle between machines

δ—Angle between voltages of machines

Δ—Per-unit torsional oscillation frequency

$\tau_2=x_2/x_m$—Leakage coefficient

ϕ—Flux per pole

Ω—Angular velocity

Appendix A. Simplified Solution of Negative Damping for a Doubly Fed Polyphase Machine

In the analysis, the following assumptions are made:

1. The primary resistance is negligible in comparison with primary reactance at the frequencies considered.

2. The three-phase winding of the machine can be replaced by an equivalent two-phase winding.

3. The coefficients of self-inductance of primary and secondary (L_1 and L_2) and of mutual inductance (M) are independent of the relative position of rotor and stator.

4. The primary flux is sinusoidally distributed, and its amplitude is the same at all times.

5. The angular motion of the rotor with respect to that of the stator flux is represented by

$$\theta=\omega_s t+f(t) \tag{1}$$

where ω_s is equal to 2π times the frequency of the a-c voltage applied to the rotor.

6. The amplitude of the angular oscillations represented by $f(t)$ is small, so that $\cos f(t)$ can be taken as unity, and $\sin f(t)$ can be taken as $f(t)$. This is a good approximation up to about 15 electrical degrees. On this basis

$$\cos\theta=\cos\omega_s t-f(t)\sin\omega_s t \tag{2}$$

$$\sin\theta=\sin\omega_s t+f(t)\cos\omega_s t \tag{3}$$

7. The systems providing excitation for stator and rotor are very large, so that they are unaffected by any change in the motor.

The flux linkages in phase a of the secondary due to primary flux are equal to

$$\psi_{a_2}'=-\frac{M\psi_1}{L_1}\cos\theta \tag{4}$$

where ψ_1 represents the primary-flux linkages. Similarly, the flux linkages in secondary phase b are

$$\psi_{b_2}'=-\frac{M\psi_1}{L_1}\sin\theta \tag{5}$$

The voltage in phase a of the secondary is

$$e_{a_2}=p\psi_{a_2}'-E_2\sin(\omega_s t-\phi) \tag{6}$$

where E_2 is the amplitude of the voltage applied to the secondary, and ϕ is the phase angle between this voltage and the voltage $p\psi_{a_2}'$. The voltage in phase b is

$$e_{b_2}=p\psi_{b_2}'+E_2\cos(\omega_s t-\phi) \tag{7}$$

In the operational form the currents in phases a and b of the secondary are

$$i_a=\frac{p\psi_{a_2}'-E_2\sin(\omega_s t-\phi)}{r+pL} \tag{8}$$

$$i_b=\frac{p\psi_{b_2}'+E_2\cos(\omega_s t-\phi)}{r+pL} \tag{9}$$

The total flux linkages of the secondary phases are

$$\psi_{a_2}=-M\frac{\psi_1}{L_1}-i_aL \tag{10}$$

$$\psi_{b_2}=-M\frac{\psi_1}{L_1}-i_bL \tag{11}$$

In equations 7 to 11, r is the resistance and L is the equivalent coefficient of self-inductance of the secondary, which is equal to

$$L=L_2-\frac{M^2}{L_1} \tag{12}$$

From the two-reaction theory of torque the torque in the machine is

$$T=i_a\psi_{b_2}-i_b\psi_{a_2}$$

$$=M\frac{\psi_1}{L_1}\sin\theta\times$$

$$\left[\frac{M\frac{\psi_1}{L_1}p\cos\theta+E_2\sin(\omega_st-\phi)}{r+pL}\right]+$$

$$M\frac{\psi_1}{L_1}\cos\theta\times$$

$$\left[\frac{-M\frac{\psi_1}{L_1}p\sin\theta+E_2\cos(\omega_st-\phi)}{r+pL}\right]\quad(13)$$

This torque equation can be expanded and simplified to equation 14.

$$T=M\frac{\psi_1}{L_1}\left[M\frac{\psi_1}{L_1}\left(\frac{-r\omega_s+f(t)\omega_s^2L}{r^2+\omega_s^2L^2}-\right.\right.$$

$$\sin\theta\frac{f(t)\omega_s\cos\omega_st+pf(t)\sin\omega_st}{r+pL}+$$

$$\left.\cos\theta\frac{f(t)\omega_s\sin\omega_st-pf(t)\cos\omega_st}{r+pL}\right)+$$

$$E_2\left(\frac{1}{r^2+\omega_s^2L^2}\right)(r\cos\phi-\omega_sL\sin\phi-$$

$$\left.f(t)\omega_sL\cos\phi-f(t)r\sin\phi)\right]\quad(14)$$

If the angular oscillation represented by $f(t)$ is set equal to $\theta_\Delta\sin\omega_\Delta t$, where θ_Δ is the amplitude of the fundamental frequency of variation and ω_Δ is 2π times this frequency, the torque is represented by equation 15, in which $\omega_+=(\omega_s+\omega_\Delta)$, $\omega_-=(\omega_s-\omega_\Delta)$, and the secondary resistance is r_+ at ω_+, r_- at ω_-, and r_s at ω_s.

$$T=\left(M\frac{\psi_1}{L_1}\right)^2\left(\frac{-r_s\omega_s}{r_s^2+\omega_s^2L^2}\right)+$$

$$M\frac{\psi_1}{L_1}E_2\left(\frac{1}{r_s^2+\omega_s^2L^2}\right)(r_s\cos\phi-\omega_sL\sin\phi)-$$

$$\left(M\frac{\psi_1}{L_1}\right)^2\left(\frac{\theta_\Delta}{2}\right)\left(\frac{\omega_+r_+}{r_+^2+\omega_+^2L^2}-\right.$$

$$\left.\frac{\omega_-r_-}{r_-^2+\omega_-^2L^2}\right)\cos\omega_\Delta t-\left(M\frac{\psi_1}{L_1}\right)\times$$

$$\left[M\frac{\psi_1}{L_1}\left(\frac{\theta_\Delta}{2}\right)\left(\frac{\omega_+^2}{r_+^2+\omega_+^2L^2}+\frac{\omega_-^2}{r_-^2+\omega_-^2L^2}-\right.\right.$$

$$\left.\left.\frac{2\omega_s^2}{r_s^2+\omega_s^2L^2}\right)L+E_2\theta_\Delta\frac{\omega_sL\cos\phi+r\sin\phi}{r_s^2+\omega_s^2L^2}\right]\times$$

$$\sin\omega_\Delta t\quad(15)$$

The $\sin\omega_\Delta t$ term of equation 15 represents a torque in phase with and opposed to the alternating displacement, and is therefore the synchronizing torque.

The $\cos\omega_\Delta t$ term represents a torque in phase with and aiding the velocity. The nature of this torque is such that it tends to increase any small variation in the motion of the rotor, and thus it provides negative damping in the machine.

This equation indicates that the damping factor at any speed can be determined from the speed-torque curve obtained with the rotor short-circuited. The slope of a line between two points on this curve is the value of the damping coefficient at the speed halfway between those points. Thus if vibration is forced at the frequency corresponding to ω_Δ, and the speed of the machine is $(\omega-\omega_s)$, the damping factor will be the slope of the line connecting points on the curve at the speeds $(\omega-\omega_s)+\omega_\Delta$ and $(\omega-\omega_s)-\omega_\Delta$.

In the practical case the secondary is not connected to a system of infinite capacity, so the secondary voltages induced by the pulsations act through a circuit which includes the impedance of the machine connected in the secondary. Since the pulsations induce oscillation frequency currents in the rotor of the connected synchronous machine, the rotor-circuit resistances must be divided by the per-unit oscillation frequency when this impedance is added into the equivalent circuit.

Appendix B. Tests on Model System

In order to study the stability of the system under consideration, a model has been set up as shown in Figure 6. The induction machine A was loaded by a d-c generator for want of a fan, but a flywheel has been put on the shaft of this d-c machine as a substitute for the high WR^2 of the fan. A variable ohmic resistance and a variable inductance have been put in the armature circuit of both d-c machines DC_1 and DC_2 in order to vary the damping torque as well as the synchronizing torque of these machines. The constant-speed set (DC_2+S_2) consisted of duplicates of the machines of the variable-speed set (DC_1+S_1) with the sole exception that the synchronous machine (S_1) had a damper winding of copper while the synchronous machine (S_2) had a damper winding of material with high resistivity.

The rating and the constants of the different machines were as follows:

Induction motor:

100 horsepower—2,200/440 volts—60 cycles—3 phases—6 poles

$r_1=1.2$ $r_2=1.19$
$x_1=4.5$ $x_2=4.0$
$x_m=147$ $WR^2=172$ pound-feet^2
Flywheel $WR^2=164$ pound-feet^2

Synchronous machines:

100 kva—2,300 volts—60 cycles—3 phases—6 poles

$r_1=1.27$ $x_{ad}=57.0$ $r_f=0.161$
$x_1=5.0$ $x_{aq}=54.3$ $x_f=6.2$
$r_{Dd}=2.76$ $r_{Dq}=1.27$
$x_{Dd}=8.3$ $x_{Dq}=4.36$
$WR^2=195$ pound-feet^2

Transformer between A and S_1:

3×33 kva—2,300/440 volts—60 cycles

$r_1=0.32$ $x_1+x_2=1.5$
$r_2=0.0734$ (including brushes)

The constants given above are expressed in ohms per phase for 60 cycles; they are reduced to the primary of each machine.

D-c machines:

150 horsepower—230 volts—525 amperes—1,100 rpm—$WR^2=305$ pound-feet^2—6 poles—6 parallels —$N=450$

Armature resistance $r_a=0.0073$
Armature reactance $x_a=0.11$ at 25 cycles

As could be expected, the tests have shown that the system is statically stable and that the speed control takes the same course as in the conventional Kramer set, where the machine that absorbs the slip power of the induction machine is a rotary converter. A continuous variation of the speed is possible without any disturbances.

In order to study the dynamic stability, the damping of the system has been artificially weakened by overexcitation of the synchronous motor (S_1) or by putting an inductance in the armature circuit of both d-c machines. Two kinds of conditions under which sustained oscillations have been observed are given in the following:

Condition *A*

Machine	Volts	Amperes	Cos ϕ	Slip
A	2,310	25.5	0.736 leading	26.6%
S_1	626	31.5	0.57 leading	
DC_1	58	75		
S_2	2,310	0		

The frequency of the oscillation was $f_o=2.5$ cycles per second. The machine (S_1) was overexcited. Between both d-c machines was placed an additional resistance $=0.024$ ohm.

For these conditions the following damping and synchronizing torques were determined:

Machine	Td (Pound-Feet-Seconds)	Ts (Pound-Feet)
S_1	−58	3,100
DC_1	62.7	1,590
S_2	9.6	885

The factor K_1 (Figure 4) is here equal to 1, the factor K_2 is equal to $\frac{1{,}200}{320}\times\frac{2.41}{0.512}=17.6$, thus the equivalent circuit and its constants are as shown in Figure 7.

Substituting the impedances of Figure 7 in the equations, the characteristic differential equation of the system can be found, and by solving this equation the tested frequency of the oscillations can be checked. The substitution leads to a differential equation of seventh degree. For the solution of such an equation Graeffe's method or the matrix method, both of which are rather laborious, has to be used. An examination of Figure 7 shows that this is not necessary in our case, where the machine (S_2) has a much higher rating than it has in the real induction synchronous cascade.

$f_o=2.5$ cycles per second corresponds to $p=15.7$. The impedance of the machine (S_2) is so large for this value of p that it has practically no influence on the impedance that represents the two d-c machines. Thus we can omit the tail of Figure 7, and the differential equation will be only of the fifth degree. A further reduction by 1 is possible, if we limit our considerations to a natural frequency around 2.5 cycles per second. It is then possible to replace both impedances in parallel that represent the two d-c machines by one impedance. With this as-

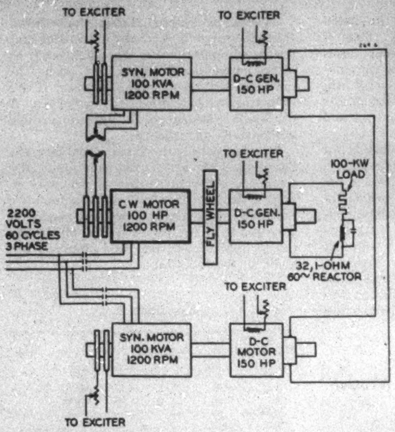

Figure 6 (left). Variable-speed synchronous Kramer-set laboratory - model test of large wind-tunnel drive

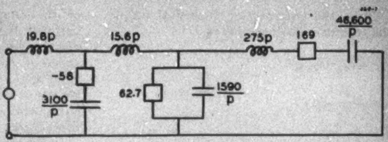

Figure 7 (upper right). Electrical analogy with numerical values corresponding to model test

sumption the following impedances will determine the oscillations:

$$Z_1=19.8 \qquad Z_2=-58+\frac{3,100}{p}$$

$$Z_3=43+15.6p+\frac{490}{p}$$

The characteristic differential equation is with these constants

$$p^4-3.89p^3+379p^2+338p+4,910=0$$

The roots of this equation are

$$p_{1,2}=2.4\pm j19$$
$$p_{3,4}=0.54\pm j3.6$$

To the angular velocity 19 corresponds $f_0=3.02$ cycles per second. Since the real part of this pair of roots is positive, the damping is negative, and the oscillations are sustained.

To the other pair of roots corresponds a slow oscillation with 0.57 cycles per second. A slower oscillation than this has been observed during the tests. It must be noticed that the damping and synchronizing torques depend on the frequency of the oscillations. The values of these torques as given above were determined for $f_0=2.7$ and are not correct for the low oscillations. Thus the second pair of roots is only approximate.

Condition *B*

Machine	Volts	Amperes	Cos ϕ	Slip
A	2,460	17.7	0.71 leading	35.4%
S_1	920	26.7	0.50 leading	
DC_1	80	90		

The frequency of the oscillations was $f_0=2.9$ cycles per second. The second d-c machine had been cut out, and the DC_1 was loaded into a resistor. A large reactance between both d-c machines had given under the same conditions the same frequency of oscillations. The machine (S_1) was overexcited ($e_d=1.95$).

For these conditions the damping and synchronizing torque of the synchronous motor (S_1) are

$$T_d=8.8 \text{ pound-feet-seconds}$$
$$T_s=3,330 \text{ pound-feet}$$

The differential equation of the oscillation is

$$p^2+1.0+382=0$$

and the roots are

$$p=-0.5\pm j19.5$$

To the angular velocity 19.5 corresponds $f_0=3.1$ cycles per second. The calculation gives a small positive damping, while the test has shown sustained oscillations, that is negative damping. The discrepancy is caused by the difficulty of exact determination of the input (and output) current, and so on, at the starting of the oscillations. The figures under conditions *A* and *B* are only approximate.

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Loss-of-Field Protection for Generators

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Synopsis: The features of a new protective relay scheme are described. The scheme is designed to remove underexcited a-c generators from the system, upon occurrence of undervoltage, before loss of synchronism occurs. The relay development and its operating characteristics are described. Calculations required to determine generator behavior after loss of field are also presented to make the application of this scheme easily applied to generators on any system.

The Problem

THOUGH electrical circuits from generator armature windings outward to utilization apparatus have received much attention from a protective viewpoint in the past 20 years, relatively little attention has been given to generator field-winding protection. Calculations show that loss of field in a large generator may cause serious voltage disturbance to the system. A need for some suitable protection method that will initiate disconnection of the troubled machine is indicated.

When loss of field occurs on a loaded generator, the magnetic coupling between the rotor and the stator becomes so weakened that the rotor advances and, after a short period, pulls out of synchronism with the system. The unit continues to carry load of varying magnitude but draws a high component of wattless current from the system.

Continued operation without excitation has harmful effects to both the generator and to the system. The generator, now an asynchronous machine, will be subject to high circulating currents in the face of the field rotor or in the amortisseur winding, and these may cause injurious heating, at least in local areas. Also induced current or voltage will appear in the field winding depending upon whether it is short circuited or open circuited.

The effects of field failure may be much more important on the system, particularly if the generator is a large unit in relation to other operating capacity. For complete loss of field, calculations have indicated that, on most power systems not equipped with automatic-generator voltage regulators, seriously low-system voltages may be reached in not more than 10 to 15 seconds and in some cases in as short a time as one second.

System loads affect the degree of voltage disturbance after loss of field. If a large percentage of the load is induction motors equipped to be disconnected only after the voltage has reached 25 to 50 per cent of normal, many of these will stall as the voltage is reduced to approximately 75 per cent of normal at the bus. This condition becoming cumulative, may cause voltage instability until the voltage has been reduced to a value where most of the motor load does become disconnected.

Since 1927 there have been five serious machine-excitation failures on the system with which the authors are connected. In addition to these, three other cases of field trouble have developed in large machines which might easily have caused loss of field, had the conditions not been discovered and corrected. One of these failures reached the stage of voltage instability, as referred to above, because of motor loads, and the station bus voltage was reduced to 37 per cent of normal before a sufficient number of motors was disconnected to permit recovery to approximately 80 per cent of normal voltage.

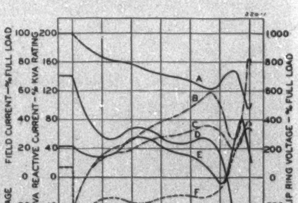

Figure 1. Generator loss-of-field characteristics after accidental tripping of field-supply breaker (large 1,800 rpm unit)

A—Bus voltage
B—Generator reactive current into generator
C—Generator reactive kilovolt-amperes into generator
D—Generator field current
E—Generator output kilowatts
F—Slip-ring voltage

It became evident some years ago that this system in New York required adequate means for main-generator field protection. The results of the development which followed, and a description of the means for applying it, are outlined in sufficient detail to permit practical use of the information by others.

Field protective relays should be sensitive to any reduction of excitation that will, at any generator load, become unsatisfactory for continued operation of the machine. Relays which operate at a specific value of field current or voltage do not give full coverage, since they must be set to operate at values well below no-load excitation conditions and yet be required to function for excitation disturbances under full-load conditions that are within safe operating range so far as the relays can determine. The new relay scheme described herein provides the degree of sensitivity desired and equally satisfies the requirements for field protection at any level of generator loading.

Generator Behavior After Loss of Field

Field loss in a generator may be partial or complete. Types of partial field failure are:

(*a*) Field rheostat trouble.
(*b*) Reductions in excitation voltage.
(*c*) Internal short circuit in a section of the field winding.
(*d*) Operating error.

Complete field failure would include:

(*a*) Winding open circuit.
(*b*) Slip ring or equivalent short circuit.

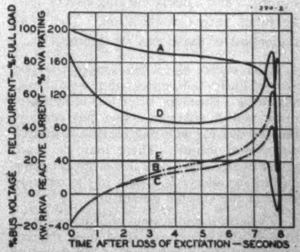

Figure 2. Generator loss-of-field characteristics after loss of field to main exciter (3,600 rpm unit)

A—Bus voltage
B—Generator reactive current into generator
C—Generator reactive kilovolt-amperes into generator
D—Generator field current
E—Generator output kilowatts

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The authors acknowledge the assistance of S. B. Crary in the method of calculation, the late L. N. Crichton on the relay development, and R. S. Quimby in connection with the voltage regulator design.

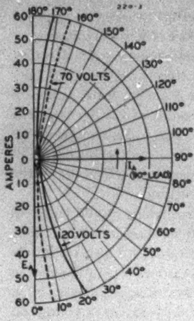


Figure 3. Phase-angle characteristics of reactive-current relay (tripping occurs in area to right of characteristic curves)

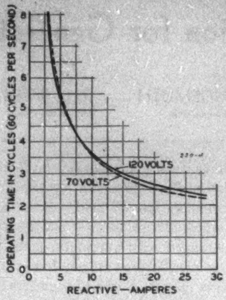


Figure 4. Time–reactive current characteristics of reactive-current relay (voltages are input values to magnetic voltage regulators)

(*c*) Loss of field to main exciter.

(*d*) Accidental opening of field supply breaker.

(*e*) Operating error.

To show the behavior of generators when complete loss of field occurs, two machines on the system with which the authors are connected, were selected. One of these shows conditions for the case of "accidental tripping of field supply breaker," and the other, "loss of field to main exciter." The machine used to show effects of the first condition is one of the largest units on the system and operates at 1,800 rpm. The curves showing the various behaviors are given in Figure 1. The generator used in the second case is a unit of medium size which operates at 3,600 rpm. Its characteristic curves are given in Figure 2.

In both cases the system was considered to be operating with normal capacity for off-peak conditions. No generator automatic voltage regulators were considered to be in service.

In calculating the effects shown in Figures 1 and 2 many varying effects must be taken into consideration. The more important of these may be itemized as:

1. The effects of turbine-governor action are not likely to be great before loss of synchronism. However, after loss of synchronism, violent governor action may occur,[1,2] thereby causing wide fluctuations in generator power output. There may be small power oscillations before loss of synchronism due to the effects of inertia in the rotating parts of the affected generator.

2. Generator and system impedances must be carefully considered since their relative values determine, in large part, the degree of voltage disturbances after loss of field. Obviously the effect on system voltage will be greater as the relative size of the machine increases.

3. The time constant of the generator field circuit determines the rate of decay in bus voltage after loss of field. The field-winding open-circuit time constant as modified by any field circuit external resistance determines, in approximately inverse porportion, the rate of decay in internal generated voltage and bus voltage.

4. Initial power output of the affected generator has an effect on the elapsed time from loss of field to loss of synchronism. Obviously the time will be shorter as the load on the generator becomes larger.

5. Initial reactive loading on the affected generator must be taken into account, since the loss of that output has its effects on the reactive output required of the other system machines. Higher reactive loading tends toward greater reduction of bus voltage after loss of field.

6. Generator voltage regulators were not considered in these calculations, since they are not used generally on the system with which the authors are concerned. If regulators are used on sufficient generating capacity, they will help materially in maintaining bus voltage when one generator has lost its field.

The methods used in calculating the changes occurring after loss of field are given in appendixes I, II, III, which are a part of this paper.

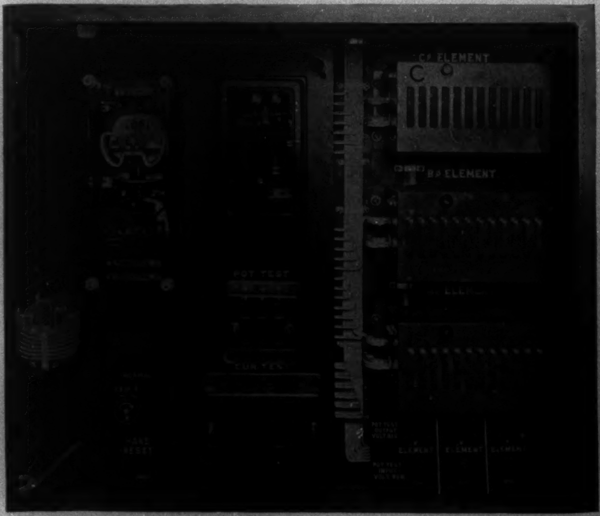
Development of Protective Equipment

The results of Figure 1 show that a relay designed to measure "input" megavars might readily be used to detect generator field excitation disturbances to the degree desired. However, as is shown here, the rate of decay of voltage is so fast for some machines that the rate of increase in "input" megavars is greatly reduced before loss of synchronism occurs. Hence, the torque and operating speed of such a relay is reduced where fast performance is desired. The rate of change in reactive current is not affected. Consequently a relay designed to measure reactive current and be sensitive to its direction would be a better instrument for this purpose. This reactive current relay must have high operating speed if it is to clear the machine before the bus voltage is reduced below the minimum permissible value.

Calculations have indicated that such a relay applied as single-phase elements would be subject to incorrect operations on unbalanced system short circuits. Hence, a polyphase relay with three elements (one per phase) was indicated.

Figure 5. Typical installation of field protective relays

A—Reactive-current relay
B—Undervoltage relay
C—Magnetic voltage regulators



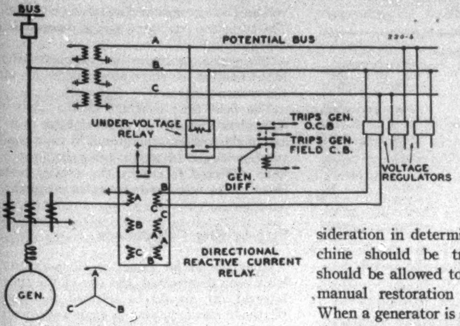


Figure 6. Schematic diagram of relay connections

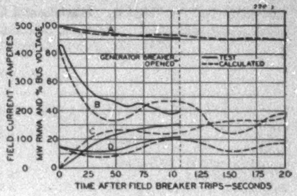


Figure 7. Loss-of-field test on a 50,000-kw generator—field breaker tripped

A—Bus voltage
B—Field current
C—Generator reactive kilovolt-amperes into generator
D—Generator output kilowatts

The relay is a polyphase induction disk, three-element self-reset watt-type device with provisions for time and wattage adjustments. Seven wattage taps varying from 50 to approximately 500 watts permit use of the same relay on any size generator.

To permit measurement of reactive current a magnetic-type voltage regulator was chosen to maintain constant voltage at the relay when variations occur in the bus voltage. The magnetic voltage regulator holds ±1 per cent of normal voltage on the relay coil and permits not more than ±5 degree phase shift while the input voltage varies from 60 per cent to 110 per cent of normal.

Care was required to assure satisfactory performance from zero to 60 cycles and over a corresponding range in voltage on the potential supply to the magnetic regulators and to the relay. Since the generator potential transformers are used for the relays, these may be energized during the generator starting period, thereby producing this wide range of frequency and voltage.

Figure 3 shows the phase-angle characteristics of the relay supplied through the magnetic voltage regulators with input voltage at 120 and at 70 volts. It will be noted that the relay maximum torque appears at about 90 degrees leading current. To obtain this characteristic the relay voltage leads the normal in-phase current by 90 degrees, as would be necessary in applying a watt element for reactive current measurements.

Figure 4 shows the time-reactive current curve of the relay when supplied at 120 and 70 volts through the magnetic voltage regulators.

It will be noted in both Figures 3 and 4 that the relay torque does not change appreciably over this wide voltage range and that the phase shift of the tripping characteristic is not great.

Bus-voltage magnitude is a major consideration in determining whether a machine should be tripped instantly or should be allowed to operate and permit manual restoration of field excitation. When a generator is small compared with the system-connected generation and is linked to the other system generators through relatively low impedance, loss of field in that machine may not cause a serious voltage disturbance. Field-current interruptions may occur or serious field-current reductions may be experienced where quick correction of the trouble is possible and the generator need not be lost from the system.

An undervoltage relay has, therefore, been made a part of the present scheme. It is a standard voltage-regulator type element adjustable to operate at the particular voltage value dictated by the characteristics of an individual machine. Its contacts are connected in series with those of the reactive current relay to prevent tripping unless the bus voltage approaches a value beyond which continued operation of the generator would be unsafe.

Figure 5 shows a typical installation of the relay equipment.

Figure 6 shows a simplified connection diagram of the relays and their associated equipment. Tripping of the generator circuit breaker is first initiated and the field supply is then disconnected by an auxiliary switch on the generator circuit breaker.

System Tests

The reactive current relay was set up for tests on a 50,000-kw machine. The unit was loaded to 14,000 kw at unity power factor. The main-field breaker was then opened, thereby connecting the field winding to its discharge resistor through the main-field rheostat. Oscillographic records were obtained of field current, megawatts and megavars. Bus voltage was obtained with a recording voltmeter. Some of the results of this test, test 1, are shown in Figure 7. In the same illustration, curves are given showing the calculated values for the same conditions. Agreement was close enough to assure that behavior after loss of field can be determined with sufficient accuracy for applying field protective relays.

Altogether four similar tests were made on two machines of the same design and rating. All initial and final behaviors observed on tests 1 and 2 are given in Table I. Complete data were not taken on the last two tests, which were made at higher initial loads.

Calculations show, for this system, that the dividing line in machine rating is about 50 to 60 megawatts beyond which loss of field may produce unstable voltage conditions for the most unfavorable operating and load conditions. Agreement has been reached to apply the protection to all machines on the system of 50-megawatts and larger except for two 60-megawatts, 25-cycle units whose characteristics are such that they could be made the "exceptions."

Conclusions

The relay equipment described makes practicable protective measures which will cover virtually all cases and types of loss of field in generators. It is believed that

Table I. Results of Loss-of-Field Tests on 50,000-Kw Generator

		Generator Armature				Field		
Test	Condition	Current	Volts	Mw	Mvar	Current	Volts	Time to Trip (Sec.)
1	Before field loss	300	26,500	14	0	+430	+ 82	
	At tripping	910	24,000	22	−31	+200	−150	1.1
2	Before field loss	500	27,000	16	+17	+625	+118	
	At tripping	900	23,800	22.5	−29	+200	−110	1,4

Tabel II. Tabulation of Calculations for Appendix III

	Time—t—Seconds				
	0	0.1	0.2	0.3	0.4
$e_g(t)$	1.129	0.974	0.855	0.763	0.688
$\Delta e_g(t)$	−0.155	−0.119	−0.092	−0.075	−0.064
$e_s(t)$	0.916	0.916	0.915	0.914	0.912
$\Delta e_s(t)$	0	−0.001	−0.001	−0.002	−0.002
$E_g(t)$ per unit	1.508	1.155	0.897	0.723	0.618
$E_g(t)$ % full load	70.5	54.0	41.9	33.8	28.9
$E_s(t)$	0.836	0.899	0.944	0.979	1.002
$P_g(t)$ per unit	0.436	0.358	0.304	0.279	0.278
$P_g(t)$ % kva rating	43.6	35.8	30.4	27.9	27.8
$Q_g(t)$	0.813	0.300	0.057	−0.039	−0.060
$P_{gi}-P_g(t)$	0	0.078	0.132	0.157	0.158
$\Delta\omega(t)$	0	1.3	2.1	2.5	2.5
$\omega(t+{}^1/_2\Delta t)$—degrees/0.1 sec.	0	1.3	3.4	5.9	8.4
$\delta(t)$—degrees	30.1	30.1	31.4	34.8	40.7
I	0.612	0.404	0.346	0.389	0.460
Bus voltage—per cent	101.5	95.0	89.8	86.5	84.1
Bus reactive kva *into* generator—% kva rating	−44.4	−13.8	+6.1	+18.9	+26.8
Bus reactive current *into* generator—% kva rating	−43.8	−14.5	+6.8	+21.9	+31.9

All values are per-unit on machine rating, 137,500 kva, and 13,500 volts except where indicated otherwise.

this further improves the reliability of electric service since a thoroughly reliable relay device has been developed which does not of itself create a hazard.

The scheme provides a relay combination which is sensitive to serious excitation reductions, regardless of initial machine load and excitation values.

This new type of field protection has now been applied to all 60-cycle generators rated 50 megawatts and above, excepting two units already provided with undercurrent-undervoltage relays, and to one large 25-cycle machine on the system with which the authors are connected. Though the first installations have been in service over a year no operating experience is yet available.

Appendix I. Calculation Method for Complete Loss of Field

Basis of Calculations

The method used is a step-by-step process and takes into account the decrement of system generator internal voltages as the unexcited machine draws magnetizing current. Although less labor in calculations would result if this decrement were neglected, the authors' calculations for some 15 generators indicate that this decrement is usually about 5 per cent at the relay operating point, and may be as much as 10 to 15 per cent at "pull-out." Consequently, it is felt that the additional labor is justified.

To simplify and reduce this work as far as possible, consistent with reasonable accuracy, several simplifying assumptions have been made. The first of these is neglecting the effects of changes in system load during loss-of-field conditions. Actually some reduction will take place and will cause the bus voltage to be slightly higher than calculated at the relay operating point.

Secondly, oscillations between system generators have been neglected, and these generators have been lumped into one equivalent machine. Since changes in system load have been neglected, this machine is considered to act as motor, absorbing the power output of the unexcited generator. While the actual system generators are in no sense motors, their effects upon the unexcited generator and its bus voltage are much the same. This assumption is often used in transient-stability calculations.[3]

Third, saturation of the magnetic circuits in the unexcited generator and in the system generators has been neglected. While such saturation in the unexcited unit acts to hasten the decay of bus voltage, the saturation in the system generators acts oppositely. Furthermore, the authors' calculations indicate that the unexcited machine flux is usually in the unsaturated region at the relay operating points.

Fourth, constant prime-mover input to the unexcited generator is assumed, since the average output is constant up to "pull-out."

Fifth, to determine the relative swinging of the unexcited generator rotor against the system, system swings are neglected and an equivalent inertia is used for the unexcited machine. This inertia may be calculated by formula 4 of appendix II.

Sixth, effects of system generator voltage regulators have been neglected.

The formulas and symbols which have been used by the authors are for steam driven turbogenerators and are given in appendix II.

Where system conditions will not permit the use of the above assumptions, determination of loss-of-field conditions becomes more complicated. However, practically all cases should be capable of solution quite readily if an a-c calculating board is used.

Determination of System Equivalent-Motor Constants

The first step is to determine which system generators are likely to be appreciably affected by loss of excitation on the unit under consideration.

The system equivalent-motor synchronous reactance, x_{ds}, defined in appendix II, is the reactance from the unexcited machine terminals to the synchronous internal voltages of the selected system generators, such voltages being considered equal and in phase. The transient reactance x_{ds}', is determined in the same way.

The system equivalent motor inertia, H_s, is the sum of the corresponding inertias of the system generators.

The field time constant of the system equivalent motor, T_s', is a weighted average of the system-generator field time constants, with greater weight being given to the units expected to supply the greater portions of the unexcited generator magnetizing current.

Step-by-Step Calculations

After the initial values of all quantities have been determined, the size of the time interval, Δt, should be selected to cause relatively small changes in angle and direct axis flux during this period. For purposes of clarity in the following explanations, it will be assumed that a 0.1-second interval has been selected.

During the first time interval, that is, from time zero to time 0.1 second, the angle δ is held constant, and only the generator direct axis flux, e_g, is changed. The value of this flux at time zero, that is, $e_{g(o)}$, is reduced by the amount $\Delta e_{g(o)}$, the change from time zero to time 0.1 second, determined from either formula 8 or 9, depending upon whether the excitation supply voltage decays gradually or whether it drops to zero instantly, as in a solid short circuit. The value of E_g to be used in this formula will, of course, be that for time zero, that is, $E_{g(o)}$. No change in e_s will occur as may be seen from formula 10.

With $e_{g(0.1)}$, $e_{s(0.1)}$ and $\delta_{(0.1)}$ thus determined for time 0.1 second, corresponding synchronous voltages $E_{g(0.1)}$ and $E_{s(0.1)}$, generator output, $P_{g(0.1)}$, and other required quantities may be determined from the formulas in appendix II.

During the next time interval, that is, from time 0.1 second to time 0.2 second, e_g, e_s, and δ will change. The change $\Delta e_{g(0.1)}$ will be determined as before, using $E_{g(0.1)}$, and will be added to $e_{g(0.1)}$ to obtain e_g at time 0.2 second, that is, $e_{g(0.2)}$. Flux $e_{s(0.2)}$ will be obtained similarly.

To obtain the new angle at time 0.2 second, $\delta_{(0.2)}$, several intermediate steps must be taken. The difference between the assumed constant input to the machine, that is, the initial output P_{gi} neglecting losses, and the output at time 0.1 second, $P_{g(0.1)}$, constitutes an accelerating or decelerating force, depending upon whether this difference is positive or negative. This force is considered as acting from time 0.05 second to time 0.15 second, and during this period will produce a change in the velocity of the generator rotor above system speed. This velocity change is designated by the symbol $\Delta\omega_{(0.1)}$ and is obtained from formula 6. The velocity at time 0.15 second is considered as an average velocity for the time interval 0.1 second to 0.2 second, and is designated by the symbol $\omega_{(0.15)}$. This velocity is equal to the algebraic sum of the above change in speed, $\Delta\omega_{(0.1)}$, and the corresponding average speed at time 0.05 second, designated by the symbol $\omega_{(0.05)}$. Since the angle δ was held constant from time zero to time 0.1 second, $\omega_{(0.05)}$ is zero, and therefore $\omega_{(0.15)}$, is equal to $\Delta\omega_{(0.1)}$.

The average velocity $\omega_{(0.15)}$ for the time interval, 0.1 second to 0.2 second, is measured in the electrical degrees per time interval, that is, degrees per 0.1 second. Consequently, $\omega_{(0.15)}$ is numerically equal to the change in the angle δ during this period. Therefore, δ at time 0.2 second, $\delta_{(0.2)}$, is equal to the angle δ at time 0.1 second, $\delta_{(0.1)}$ plus $\omega_{(0.15)}$.

The angle at time 0.3 second is obtained similarly. Velocity $\omega_{(0.25)}$ is equal to $\omega_{(0.15)}$ plus $\Delta\omega_{(0.2)}$, and $\delta_{(0.3)}$ is equal to $\delta_{(0.2)}$ plus $\omega_{(0.25)}$.

All subsequent step-by-step calculations are made by a continuation of this method.

These steps are shown in detail by the sample calculations of appendix III for the generator of Figure 1.

Other quantities, such as generator terminal voltage and magnetizing current, may easily be obtained for each time interval by the usual vector calculations.

Inasmuch as each step of calculations depends upon the previous step, the accuracy at each step should be checked as far as possible.

Simplifications When T_{gf}' Is Large and H_g' Is Small

When the ratio of the generator-field time constant, T_{gf}', to the generator equivalent inertia, H_g', is around 2.0 or greater, the generator-output power oscillations are small and may be neglected without appreciable error. Consequently, as the flux decays, the machine advances by just enough to hold constant output.

Therefore, the procedure here consists in determining e_g and e_s for each step as before, and then determining by trial-and-error process the angle δ which will produce the constant output. Although two or three trials may be necessary for each step, the number of steps to be calculated will be considerably less than required by the previously outlined method of step increments of velocity and velocity change, because much larger time intervals may be used.

Appendix II. Calculation Nomenclature and Formulas

Nomenclature

E_g = Generator voltage behind synchronous reactance x_{dg} (per-unit field current—saturation neglected).

e_g = Generator voltage corresponding to direct-axis flux (per-unit generator direct-axis flux).

x_{dg} = Generator direct-axis synchronous reactance.

x_{dg}' = Generator direct-axis transient reactance.

I = Generator output current.

δ = Angle between generator and system equivalent motor—electrical degrees.

P_g = Generator power output to system.

Q_g = Generator reactive kilovolt-amperes supplied to system and corresponding to voltage E_g and current I.

E_s = System equivalent-motor voltage behind synchronous reactance x_{ds} (per-unit field current—saturation neglected).

e_s = System equivalent-motor voltage corresponding to direct-axis flux (per-unit motor direct-axis flux).

x_{ds} = Direct-axis synchronous reactance of system equivalent motor plus system impedance between this motor and the generator.

x_{ds}' = Direct-axis transient reactance of system equivalent motor plus system impedance between this motor and the generator.

$x_{gs} = x_{dg} + x_{ds}$.

T_{gdo}' = Open-circuit time constant of generator—seconds.

T_{gf}' = Field-circuit time constant of generator during loss-of-field conditions—seconds.

T_{sf}' = Field-circuit time constant of system equivalent motor during loss-of-field conditions—seconds.

t = Time in seconds.

Δt = Time interval selected for step-by-step calculations—seconds.

H_g = Inertia of generator and turbine rotors—kilowatt seconds of generator and turbine rotors divided by the kilovolt-ampere base used in calculations.

H_s = Inertia of system equivalent motor—kilowatt-seconds of system equivalent motor divided by kilovolt-ampere base used in calculations.

H_g' = Equivalent inertia of generator and turbine rotors relative to an infinite system inertia—kilowatt-seconds divided by kilovolt-ampere base used in calculations.

K_g' = Generator acceleration constant corresponding to H_g'.

E_{si} = Initial value of E_s before generator loss of excitation.

P_{gi} = Initial value of P_g before loss of excitation.

f = System frequency—cycles per second.

$\delta_{(t)} = \delta$ at time t.

$E_{g(t)} = E_g$ at time t.

$E_{s(t)} = E_s$ at time t.

$e_{g(t)} = e_g$ at time t.

$e_{s(t)} = e_s$ at time t.

$\Delta e_{g(t)}$ = Change in e_g during time interval Δt between time t and time $(t+\Delta t)$.

$\Delta e_{s(t)}$ = Change in e_s during time interval Δt between time t and time $(t+\Delta t)$.

$P_{g(t)}$ = Generator power output at time t.

$Q_{g(t)} = Q_g$ at time t.

$\omega_{(t+{}^1/_2\Delta t)}$ = Average velocity of generator rotor above system speed during time interval Δt between time t and time $(t+\Delta t)$—electrical degrees per time interval Δt.

$\omega_{(t-{}^1/_2\Delta t)}$ = Average velocity of generator rotor above system speed during the time interval Δt between time $(t-\Delta t)$ and time t—electrical degrees per time interval Δt.

$\Delta\omega_{(t)}$ = Change in velocity of generator rotor from the average velocity $\omega_{(t-{}^1/_2\Delta t)}$ to the average velocity $\omega_{(t+{}^1/_2\Delta t)}$—electrical degrees per (time interval Δt)^2.

$E_{gf(t)}$ = Average generator per-unit field current corresponding to average exciter output voltage during time interval between time t and time $(t+\Delta t)$, divided by the generator field circuit resistance.

All quantities are per-unit quantities on the selected system kilovolt-ampere base unless otherwise indicated.

Formulas

FIELD CURRENT—DIRECT-AXIS FLUX

$$E_g = \frac{e_g - \dfrac{(x_{dg}-x_{dg}')e_s\cos\delta}{x_{gs}-(x_{ds}-x_{ds}')}}{1-\dfrac{x_{dg}-x_{dg}'}{x_{gs}}\left[1+\dfrac{(x_{ds}-x_{ds}')(\cos\delta)^2}{x_{gs}-(x_{ds}-x_{ds}')}\right]} \tag{1}$$

$$E_s = \frac{e_s - \dfrac{(x_{ds}-x_{ds}')e_g\cos\delta}{x_{gs}-(x_{dg}-x_{dg}')}}{1-\dfrac{x_{ds}-x_{ds}'}{x_{gs}}\left[1+\dfrac{(x_{dg}-x_{dg}')(\cos\delta)^2}{x_{gs}-(x_{dg}-x_{dg}')}\right]} \tag{2}$$

REAL AND REACTIVE POWER

$$P_g + jQ_g = \frac{E_gE_s}{x_{gs}}\sin\delta + j\left(\frac{E_g^2}{x_{gs}} - \frac{E_gE_s}{x_{gs}}\cos\delta\right) \tag{3}$$

INERTIA

$$H_g' = \frac{H_gH_s}{H_g+H_s} \tag{4}$$

GENERATOR ACCELERATION CONSTANT AND VELOCITY

$$K_g' = \frac{180f(\Delta t)^2}{H_g'} \tag{5}$$

$$\Delta\omega_{(t)} = K_g'(P_{gi}-P_{g(t)}) \tag{6}$$

$$\omega_{(t+{}^1/_2\Delta t)} = \omega_{(t-{}^1/_2\Delta t)} + \Delta\omega_{(t)} \tag{7}$$

FIELD DECREMENT—GENERATOR

When change in exciter voltage during the time interval Δt must be considered.

$$\Delta e_{g(t)} = \frac{E_{gf(t)}-E_{g(t)}}{T_{gf}'}\Delta t \tag{8}$$

When excitation voltage supplied to the generator field may be considered as zero during the time interval Δt,

$$\Delta e_{g(t)} = \frac{-E_{g(t)}}{T_{gf}'}\Delta t \tag{9}$$

FIELD DECREMENT—SYSTEM EQUIVALENT MOTOR

$$\Delta e_{s(t)} = \frac{E_{si}-E_{s(t)}}{T_{sf}'}\Delta t \tag{10}$$

Appendix III. Sample Calculations for Generator of Figure 1

GENERATOR RATING

137,500 kva	13,800 volts
110,000 kw	60 cycles
80 per cent power factor	1,800 rpm

Full-load field current = 1,540 amperes
Per-unit field current = 720 amperes
Prime mover—medium-pressure steam turbine

Field Investigation of the Characteristics of Lightning Currents Discharged by Arresters

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Synopsis: Data have been obtained during the past three years on the magnitude and wave shape of lightning currents discharged by arresters in service on several solidly grounded neutral circuits of the American Gas and Electric Company system. Correlated measurements have been obtained with the cathode-ray oscillograph, the fulchronograph, and the surge-front recorder. The maximum arrester-phase leg current recorded in this investigation was 9,600 amperes with 70 per cent of the currents less than 1,000 amperes. The wave fronts of the low-magnitude currents were, in general, abrupt. For crest magnitudes of over 1,000 amperes they ranged from two to over 25 microseconds to crest. The maximum rate of rise recorded was 2,500 amperes per microsecond.

The components in all discharges were of relatively short duration, with times to half value averaging 25 microseconds and with no measurable durations in excess of 500 microseconds.

Of the 18 arrester-phase legs studied, all but one discharged at least once during the investigation. Nineteen of the 21 separate records of discharges in three-phase arrester banks had currents which, if arresters had not been installed, would have produced voltages in excess of the standard basic impulse level for the voltage class of the apparatus involved, so that failure of unprotected equipment might have occurred.

Purpose and Scope

THIS investigation was undertaken in 1939 to determine the lightning duty imposed on arresters in service. Factors that affect this are frequency of occurrence, magnitude, wave shape, and multiple character of the discharges. Five measuring stations were set up at different three-phase arrester locations, two in substations of The Ohio Power Company in Ohio, and three in substations of the Appalachian Electric Power Company in Virginia. One of the latter locations was moved during the course of the investigation so that in all, a total of six arrester banks was studied.

Description of System and Recording Installations

A summary of the pertinent characteristics of the substations at which the recording installations were made is given in Table I. They are located in regions where lightning was known to be frequent and at arrester banks where past experiences had indicated that a higher than average number of discharges was to be expected. The power sources feeding the circuits on which the study was made are solidly grounded.

The two recording stations on The Ohio Power Company system are at 33-kv delta-connected transformers. The installations on the Appalachian Electric Power company system are made in one similar 33-kv station; in two 12-kv stations, one with grounded-neutral and the other with delta-connected transformers; and in one 132-kv station with delta-connected transformers. The station grounds and earth resistivity are low at all of these locations.

Pictures and a schematic diagram of the recording equipment are shown in Figures 1 and 2. Each installation consists of a high-speed fulchronograph, and surge-crest ammeter links in each arrester phase lead together with a slow-speed fulchronograph, crest ammeter links, and a magnetic surge-front recorder in the common ground lead of the three arrester phases. In two cases (Twin City and Stone Creek), a cathode-ray oscillograph is also connected in the common lead. The cathode-ray oscillograph equipment was designed and built especially for the purpose of automatically recording lightning transients. It consists essentially of a glass-envelope hot-cathode oscillograph tube, the necessary electrical circuits, a special camera to photograph the trace produced by the transient on the fluorescent screen, and a current shunt. These recording instruments have been described in the literature.[1,2]

The field installations were serviced on an average of once a week during the lightning season, and usually after each lightning storm.

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This investigation has been made possible by the co-operative efforts of the two companies with which the authors are associated, as well as a number of individuals. The authors acknowledge the contributions of the field organizations of The Ohio Power Company and the Appalachian Electric Power Company in installing and servicing the field installations, and of O. Ackermann of the Westinghouse Electric and Manufacturing Company for his assistance in the design and supervision of the cathode-ray oscillographs.

GENERATOR AND SYSTEM CONSTANTS

$x_{dg} = 0.940$ $T_{gdo}' = 6.26$ seconds
$x_{dg}' = 0.241$ $T_{gf}' = 0.975$ second (corresponding to field resistance of 0.138 ohm and external resistance of 0.747 ohm)
$x_{ds} = 0.511$ $T_{sf}' = 9.0$ seconds
$x_{ds}' = 0.264$ $H_s = 53.4$ seconds
$x_{gs} = 1.451$ $H_g = 7.7$ seconds
$\Delta t = 0.1$ second $H_g' = 6.73$ seconds

Reactance—generator bus to generator terminals $= 0.048$

All values are on machine rating 137,500 kva and 13,500 volts. Reactances are per unit.

Formulas

$$E_g = \frac{e_g - 0.582 e_s \cos\delta}{0.518 - 0.099(\cos\delta)^2}$$

$$E_s = \frac{e_s - 0\ 329 e_g \cos\delta}{0.830 - 0.159(\cos\delta)^2}$$

$$P_g + jQ_g = 0.689 E_g E_s \sin\delta + j(0.689 E_g^2 - 0.689 E_g E_s \cos\delta)$$

$$\Delta\omega_{(t)} = 16.0(0.436 - P_{g(t)}) \text{ since } P_{gt} = 0.436$$

$$\Delta e_{g(t)} = -0.103 E_{g(t)}$$

$$\Delta e_{s(t)} = \frac{0.836 - E_{s(t)}}{90} \text{ since } E_{st} = 0.836$$

The values calculated with the preceding formulas are listed in Table II.

References

1. GOVERNOR PERFORMANCE DURING SYSTEM DISTURBANCES, R. C. Buell, R. J. Caughey, E. M. Hunter and V. M. Marquis. AIEE TRANSACTIONS, volume 50, 1931, pages 354–69.

2. RE-ESTABLISHING EXCITATION OF A LOADED ALTERNATOR IN PARALLEL WITH OTHERS, D. D. Higgins and E. Wild. AIEE TRANSACTIONS, volume 50, 1931, pages 1194–1200.

3. THE CALCULATION OF ALTERNATOR SWING CURVES—THE STEP-BY-STEP METHOD, F. R. Longley. AIEE TRANSACTIONS volume 49, 1930, pages 1129–51.

General Discussion of Records

Twenty-one separate sets of records have been obtained involving 49 single-phase arrester-discharge currents with a total of 88 individual components. Records were obtained at all six stations and their distribution is shown in the lower part of Table I and in the top curve of Figure 3. As shown in Figure 3, more discharges per arrester year were obtained than in other previously reported investigations. This is because the locations were chosen from past experience which indicated that they would yield more than the average number of records. The curves in this figure show considerable variation from each other. This is probably due to a number of factors including variance in lightning frequency, the number of arresters in multiple, and the type of line construction.

Of the 21 separate records, ten involved discharges in three phases, eight in two phases, and three in only one phase. Sixteen of the records showed only single-component discharges in each of the individual arrester phases. In the remaining five, which had multiple components in one or more phases, the number of components varied from two to 12. Of the 21 records, all but three had maximum crest magnitudes in the common lead which were substantially equal to the sum of the maximum crests in the individual phases.

In 12 of the records all of the components were entirely negative. Four records were entirely positive. For two records the components in two of the phases were entirely negative, while the components in the third phase were entirely positive. One record had nonoscillatory components of both polarities in the same phase. Two of the records had oscillatory components. Twenty-three of the 88 individual components, or 26 per cent, were initially positive.

Table I. Summary of Installations

	The Ohio Power Company		The Appalachian Electric Power Company			
	Twin City Substation	Stone Creek Substation	Huntington Court Substation	Christiansburg Substation	Reusens Substation	Rocky Mount Substation
Type of station	Delta	Delta	Grounded wye	Delta	Delta	Delta
Miles to nearest grounded source	20	10	0	0.6	82	25
Circuit voltage	33 kv	33 kv	12 kv	12 kv	132 kv	33 kv
Arrester type	Line	Line	Station	Line	Station	Station
Arrester rating	30 kv	30 kv	12 kv	12 kv	109 kv	30 kv
Arrester ground resistance—ohms	0.5	1.8	0.7		0.4	0.2
Earth resistivity in meter ohms	15–40	15–70	55–220		45–250	120–200
Number of lines	2	2	2	1	2	1
Type of lines	Wood	Wood	Wood	Wood	Steel—1 ground wire	Wood
Number of years of study	2	2	3	2	2	1/
Number of composite 3-phase records	9	3	2	1	2	4
Apparatus standard basic impulse level, kv	200	200	110	110	650	200
Number of records exceeding basic impulse level if no arresters	9	3	1	1	1	4
Number of discharges per individual arrester phase	21	8	2	3	5	10
Average discharges per year, phase 1	3.0	1.5	0.3	0.5	1.0	8.0
Average discharges per year, phase 2	3.5	1.0	0.3	0.5	0.5	6.0
Average discharges per year, phase 3	4.0	1.5	0	0.5	1.0	6.0

Detailed Discussion of Particular Records

A few of the records which have been obtained are discussed in detail because of their special interest. In considering the fulchronograms, the limit of sensitivity of the fulchronograph should be borne in mind. Its lower range of current sensitivity is about 50 amperes. In addition, the wave shape of arrester-discharge currents with times to half value of less than about 20 microseconds will not be recorded. Therefore, in the case of currents of short time to half value or of short duration, the instrument will indicate that they were short but will not supply accurate information on the times involved. Attention is called to this because most of the components of discharge currents recorded in this investigation were of short duration.

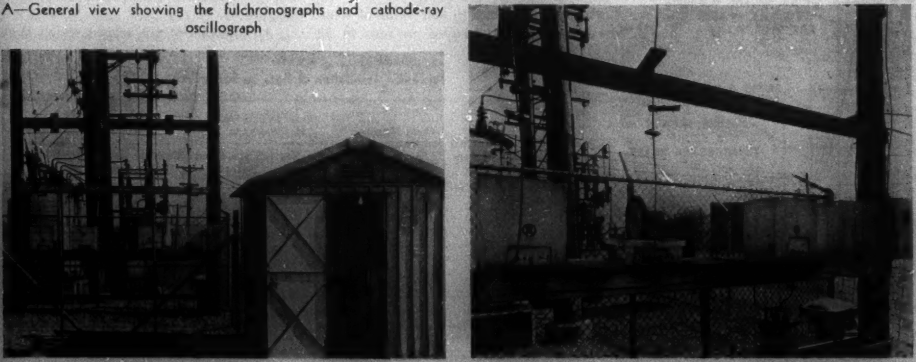


Figure 1. Lightning recording station at the Twin City Substation of The Ohio Power Company

A—General view showing the fulchronographs and cathode-ray oscillograph

B—Close-up of the fulchronographs and cathode-ray oscillograph shunt

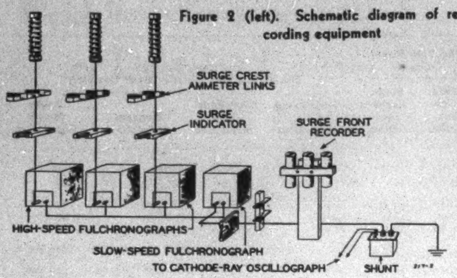


Figure 2 (left). Schematic diagram of recording equipment

SURGE CREST AMMETER LINKS
SURGE INDICATOR
SURGE FRONT RECORDER
HIGH-SPEED FULCHRONOGRAPHS
SLOW-SPEED FULCHRONOGRAPH
TO CATHODE-RAY OSCILLOGRAPH
SHUNT

Record 71, Figure 4. This record represents the first case in which directly comparable simultaneous field records of current were obtained with the fulchronographs and the cathode-ray oscillograph. Since only one arrester phase discharged, the same current passed through one high-speed fulchronograph, the low-speed fulchronograph, and the cathode-ray oscillograph, permitting a direct comparison of their records.

The current rose abruptly in 2.5 microseconds to 250 amperes and reached its crest of 500 amperes in 20 microseconds. It decayed to half value in 33 microseconds. After decaying to 100 amperes in 45 microseconds, it persisted at about this value until it decayed below the recording range of the fulchronograph in 175 microseconds. In 2,750 microseconds it again rose to about 100 amperes and lasted to 2,900 microseconds. This second portion might either be power follow or lightning current.

The cathode-ray oscillogram of this record illustrates the effect of the arrester breakdown on the wave shape of the front of the discharge current. After the gap breaks down, there is a rapid rise of current through the arrester brought about by the change in the surge circuit. This rate of change, measured in the arrester circuit during the period of readjustment, is greater than that which would exist in the line surge if no arrester discharge had occurred. Thus, the wave front of the original surge is more nearly as shown by the broken line of Figure 4b.

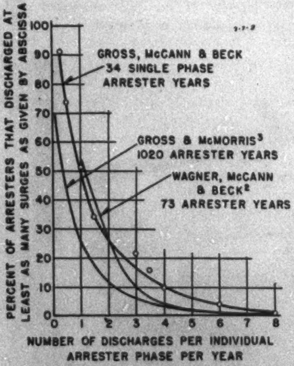


Figure 3. Weighted distribution of lightning discharges through individual arresters

Record 72, Figure 5. Only the cathode-ray oscillogram is shown for this record, since the duration is so short that only one link of each of the high-speed fulchronographs was magnetized. For this record, the surge-crest ammeter links, the slow-speed fulchronograph, and the cathode-ray oscillograph records indicate a crest magnitude of 1,000 amperes in the common ground lead. Surge-crest ammeter links and high-speed fulchronographs in the individual phase legs indicate currents totalling 550 amperes which is considerably less than the recorded neutral current. The reason for this is unknown and as pointed out previously, such a discrepancy has occurred in only a very few cases.

The neutral current has an abrupt front of less than one quarter of a microsecond and a time to half value of 8 microseconds. It decayed to zero in 32 microseconds and then rose to about 30 amperes after 66 microseconds.

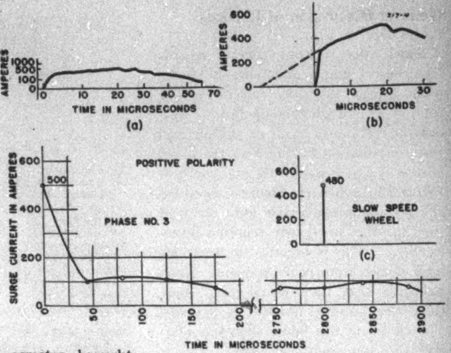


Figure 4. Record 71 obtained at the Twin City substation, May 8, 1941

(a) Cathode-ray oscillogram of current in common ground of lightning arrester

(b) Replot of oscillogram to a linear scale

(c) Slow-speed fulchronogram of current in common ground

(d) High-speed fulchronogram of current in phase 3

Record 77, Figure 6. This record is of particular interest. It gives the first cathode-ray oscillograph record of a lightning-arrester discharge current of considerable magnitude. It shows in detail the entire duration of the discharge. In addition, it is substantiated by fulchronograph records in each of the three poles and the neutral of the arrester. It was obtained during the failure of a defective bushing in the station.

Because of its importance, the original oscillogram shown in Figure 6a has been replotted to linear co-ordinates in Figure 6b. The fulchronograms are plotted in Figure 6c.

An analysis of the record shows that

Table II. Tabulation of Data on Surge Fronts*

Phases Discharged	Record Number	Nominal System Kv	Crest Magnitude in Amperes Average of Instruments	Time to Crest in Microseconds		Average Rate of Rise in Amperes Per Microsecond	
				Cathode-Ray Oscillograph	Surge-Front Recorder	Cathode-Ray Oscillograph	Surge-Front Recorder
3	92	33	−9,600		3.7		2,500
3	77	33	−6,500	3.5	4.4	1,850	1,480
2	87	33	−5,150		8.8		585
3	73	33	−3,650		6.6		555
2	91	132	−3,150		Over 25		Less 125
3	66	33	−2,[illegible]		8.3		240
3	55	33	−1,[illegible]		2		800

Of 11 additional records of surges with crest magnitudes less than 1,000 amperes, all had surge fronts of less than one microsecond as indicated by seven cathode-ray oscillograms and six surge-front recorded records, except the surge of record 71, Figure 4, which had an effective front of 2.5 microseconds.

* These measurements were made in the common ground lead of three-phase arrester banks.

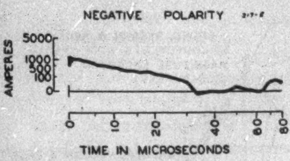
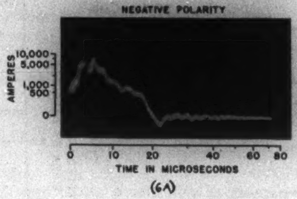


Figure 5. Cathode-ray oscillogram of record 72 obtained in common lead of arrester bank at the Twin City substation, May 22, 1941

The fulchronographs and surge-crest ammeter links both recorded single negative crest magnitudes of zero amperes in phase 1, 50 amperes in phase 2, 500 amperes in phase 3, and 1,000 amperes in the common ground. The high-speed fulchronographs showed no measurable duration



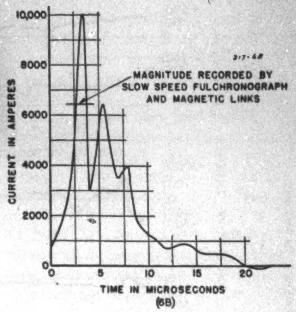


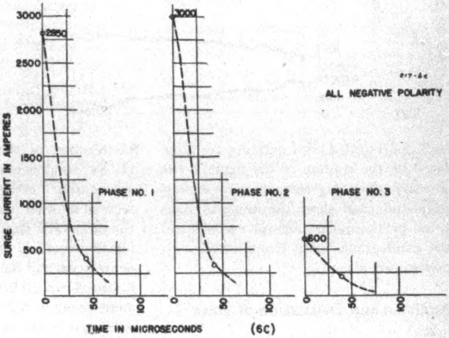
Figure 6. Record 77 obtained at the Stone Creek substation, June 12, 1941

6A Cathode-ray oscillogram of current in common ground lead of arrester bank

6B Replot of oscillogram to a linear scale

6C High-speed fulchronograms of current in each arrester phase

Slow-speed fulchronograph recorded a negative crest of 6,500 amperes and the same crest magnitudes as recorded by the fulchronographs were recorded by the surge-crest ammeter links

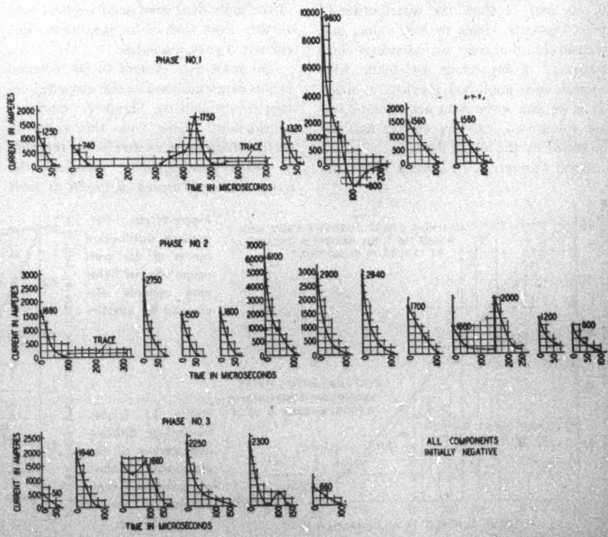


the three-phase legs of the arrester discharged simultaneously for the sum of the crest currents recorded by the fulchronographs and the surge-crest ammeter links in each phase equals the crest magnitude of 6,500 amperes recorded by the fulchronograph and surge-crest ammeter links in the common ground lead. The cathode-ray oscillogram showed a peak current of 10,000 amperes with a high oscillation believed to have been caused by the bushing failure. Its mean value of 6,500 amperes is in complete agreement with the records of the fulchronographs and magnetic links. The shape of the tail of the wave was probably affected by the bushing failure and, therefore, should not be taken as indicative of the normal arrester discharge.

Record 94, Figure 7. This record consists only of the high-speed fulchronograms in the individual phase legs of the arrester, as the slow-speed fulchronograph and the oscillograph in the common ground lead were not in operation. The record is of interest, because of the considerable number of components in all phases, 7 in phase 1, 12 in phase 2, and 6 or 7 in phase 3. The third component in phase 3 may actually be two superimposed on each other. The crest currents ranged from a maximum of 9,600 amperes, the highest recorded in this investigation, to a minimum of 510 amperes. The maximum measurable duration of any component was 400 microseconds. All of the components were totally negative, with the exception of the 9,600 ampere component which was initially negative followed by a positive portion with a crest of 800 amperes.

Record 96, Figure 8. This figure shows cathode-ray oscillograms of current in the common ground lead. The duration as measurable on the fulchronograms was too short to warrant plotting,

Figure 7 (below). High-speed fulchronograms of the current in each lightning arrester phase for record 94 obtained July 18, 1941 at the Twin City substation



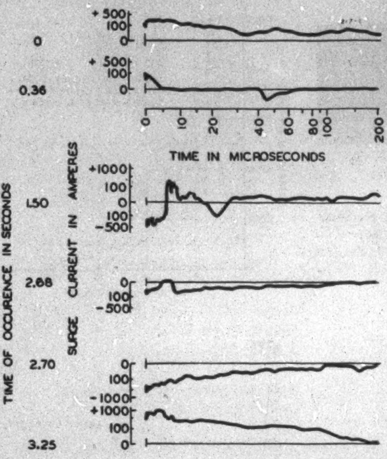


Figure 8 (left). Cathode-ray oscillograms of record 96 obtained in common ground lead of arrester bank at the Stone Creek substation, July 28, 1941

The fulchronographs recorded components of no measurable duration and the following crest magnitudes:

Phase 1 +150 +trace
−100 −200 +600

Phase 2 No current

Phase 3 +150 +trace
−100 −300 +450

Neutral +300 +trace
−100 −500 +1000

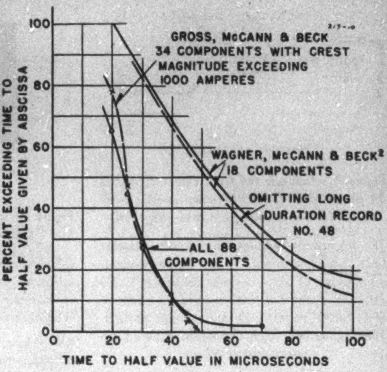


Figure 10. Percentage distribution-curves of times to half value of individual components of lightning currents discharged by arresters

but the recorded crest currents are tabulated in the caption of the figure. The arrester phase leg currents were of low magnitude and short duration, as indicated by the fulchronograph records and the oscillograms. The fronts of all components are abrupt.

Analysis and Discussion of Data

The records obtained have yielded considerable information on the magnitudes and wave shapes of currents discharged by arresters in service. Data on wave fronts are given in Table II and Figures 9, 10, and 11 show the distribution of crest currents, times to half value, and measurable durations of individual components. Comparisons are made with similar data published previously, since, as more and more data accumulate and are integrated, existing curves may be modified by the added data.

Crest Currents. Figure 9 shows the distribution of the crest magnitudes of all 88 components recorded in this investigation. For comparison with data secured in other investigations made with the surge-crest ammeter link only, a curve for the crests of the maximum components in each of the 49 arrester discharges is also shown. There is substantial agreement between this curve and the curve published by Gross and McMorris.[3] The deviations between the curves in the region of the low currents are probably the result of differences in instrument sensitivity. It is of interest that the percentage distribution of the crest magnitudes of the individual components differs only slightly from that of the maximum crest current of each discharge.

The maximum current so far recorded in this investigation is 9,600 amperes, obtained in record 94, Figure 7. Currents considerably higher than this value do occur infrequently in service as reported by other investigators; therefore, the upper limit of current in Figure 9 must be considered with reservations for the time being.

Wave Fronts. Table II lists the data obtained on wave fronts by means of the magnetic surge-front recorder and the cathode-ray oscillograph. They indicate that the low current discharges have abrupt fronts, usually less than a microsecond. This is to be expected, since for a short period following the sudden breakdown of the arrester gap, the current rises quickly as discussed for record 71, Figure 4. The low current discharges thus give little information on the fronts of the surges occurring on the line. For currents in excess of 1,000 amperes the fronts vary between 2 and 25 microseconds with average rates of rise of from less than 125 to 2,500 amperes per microsecond. The highest rate of rise reported by Wagner, McCann, and Beck[2] is 3,200 amperes per microsecond. Considering the number of measurements (29) of wave front reported in these two papers, the percentage of arrester discharges hav-

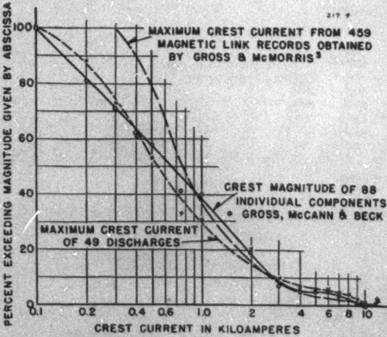


Figure 9 (left). Percentage distribution curves of the crest magnitudes of lightning currents discharged by arresters

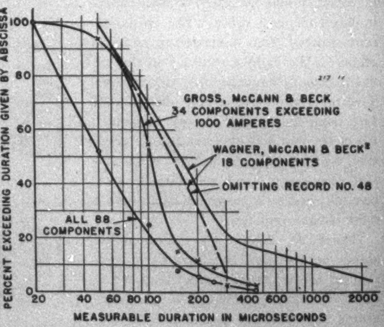


Figure 11 (right). Percentage distribution curves of the measurable duration of individual components of arrester discharges

ing rates of rise greater than 3,200 amperes per microsecond will be small.

Times to Half Value. Figure 10 shows the percentage distribution of times to half value plotted on two bases:

1. Considering all 88 components.
2. Considering only the 34 components that exceeded 1,000 amperes.

This was done because the time to half value of the small magnitude components might be influenced by the fulchronograph's lower limit of sensitivity. However, both curves are in good agreement but indicate considerably shorter times to half value than the curves published by Wagner, McCann, and Beck covering measurements on a number of systems prior to 1941. It should be noted that the Wagner, McCann, and Beck curves include only the few records secured on the American Gas and Electric Company system before 1941.

Measurable Duration. Percentage distribution curves of the measurable duration of arrester-discharge currents are given in Figure 11. This duration is the time required for the current in a component to decay below about 50 amperes. Since the measurable duration should be greatly influenced by the crest magnitude of the surge, the data have been plotted both for all 88 components and for the 34 exceeding 1,000 amperes. As would be expected, longer durations are recorded for the higher magnitude discharges. The longest duration obtained in this investigation is below 500 microseconds. This corresponds to the results reported by Wagner, McCann, and Beck if the long-duration surge obtained by them on an ungrounded system is omitted. This was expected since the present study has been conducted entirely on solidly grounded neutral systems. As has been previously pointed out,[2,4] the windings of grounded-neutral transformer banks providing a path to ground in parallel with the arrester, are more likely to absorb the low-magnitude, long-duration portions of a surge than are the arresters.

Arrester Protection. The number of arrester discharges which were associated with surges that would have produced dangerous potentials in the absence of the arresters have been estimated and listed in Table I. If one or more arrester current components in one or more phases of any discharge had a crest magnitude which indicated that the incoming surge on the line would have produced a voltage at the station in excess of the standard basic impulse level, with no arresters present, it was counted as one possible failure. Nineteen of the total of 21 records had currents above the critical value. At Twin City, Stone Creek, and Rocky Mount, all discharges were in the danger zone. At every station, at least one surge appeared that might have caused damage.

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The Fundamentals of Industrial Distribution Systems

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Synopsis: The intensive manufacturing activity accompanying the present defense program has stimulated interest in industrial electric power distribution. The ultimate objective is one of effectively satisfying electric power requirements with reasonable first cost consistent with a fair standard of safety and service reliability.

This paper presents the fundamental aspects of industrial power distribution, with particular reference to low-voltage power supply to distributed electrical machinery, and compares various basic system designs, including large concentrated substations and distributed load-center substations with radial and secondary network modifications, as to safety, service reliability, simplicity, and so on, relative to estimated installed cost. The comparative analysis comprehends the complete electrical system between high-voltage supply bus and utilization terminal of low-voltage feeders. The ideal size of unit substation as influenced by operating voltage and load density is covered.

Principles of system design here disclosed are applicable not only to new plant construction but to expansion or modernization of existing plants as well.

THE intent of this paper is to present the comparative merits of the several typical forms of industrial plant electrical distribution systems presented with a view to the general adoption of that system which will, in the majority of cases, meet the overall requirements most effectively.

General Considerations

The characteristic features on which the evaluation of merit depend are:

First cost.
Safety.
Service reliability.
Simplicity of operation.
Voltage regulation.
Efficiency.
Maintenance cost.
Flexibility in meeting load changes.

Only the *over-all* system performance is significant. This requires simultaneous recognition of all elements between the primary power source and the ultimate utilization machines. These elements are: primary feeder switching and transmission circuits, step-down transformers together with their associated high-voltage and low-voltage switching equipment, low-voltage circuits to secondary distribution centers, secondary distribution panels and branch circuits to individual loads.

Primary Operating Voltage

The primary operating voltage will, in general, be fixed by considerations outside the general factory requirements. The presence of a considerable number of large motors may favor the use of 2,400 or 4,160-volt primary systems. It will otherwise be generally preferable to adopt the incoming utility-system voltage so long as it is not in excess of 15 kv. Common system voltages are 4,160 and 13,200 volts.

Secondary Operating Voltage

The great majority of manufacturing plants with which we are concerned incorporate machine tools powered by relatively small motors, usually not in excess of 25 horsepower. Except for very small fractional horsepower units, the adoption of a nominal 440-volt level is to be preferred to lower voltages. Advantage can thus be taken of substantial reductions in secondary switching cost and size together with corresponding reductions in secondary cable cost.

The use of four-wire system suitable for 440-volt motors offers a promising means of supplying both power and fluorescent lighting from a common system retaining the advantages of 440-volt operation.

Incandescent lighting, while of ever lessening importance for factory lighting, may be encountered, and is suited to 120-volt operation. This operating voltage may be derived from 440-volt system by transformers, or by separate lighting transformers independent of the power system.

A load composed largely of incandescent lights and small motors unsuited to 440-volt service would favor the use of a 208Y/120-volt system.

Load Density

The load densities commonly encountered, including both power and lighting, are in the range of about 8 to 25 volt-amperes per square foot.

Lighting load levels for up-to-date illumination intensity may be expected to be 4 to 6 volt-amperes per square foot for incandescent lamps and 2 to 3 volt-amperes per square foot for high power-factor fluorescent lamps.

Power-load densities will be subject to greater variation ranging between about 6 and 20 volt-amperes per square foot. Areas devoted largely to assembly will show the lower load levels while intensive manufacturing areas will show the higher levels.

Accumulated records indicate a fairly limited load-density range in the order of 8 to 15 volt-amperes per square foot for existing manufacturing plants, and the higher contemplated values apply to proposed new plants. Plants of higher load density are known, but this fact does not affect the following discussion.

Specific System Comparison

To avoid abstract comparisons, a representative distributed load block has been selected totaling 3,600 to 3,750 kva with a load density of 10 volt-amperes per square foot and suited to 440-volt utilization. Individual control of low-voltage radial feeders, averaging 150 kva each, is to be provided in all cases. The primary service is considered to be 4,160-volt, three-phase, 60-cycle, with a short-circuit interrupting requirement of 150,000 kva.

Evaluations of comparative installed cost includes all electrical equipment between the main high-voltage power supply bus and the terminals of the 150 kva low-voltage feeders.

The typical forms of distribution systems have been classified as follows:

A. Single large substation—radial low-voltage feeders.
B. Distributed load-center system.
1. Simple radial (Figure 4).
2. Primary selective (Figure 5).
3. Secondary selective (Figure 7).
4. Secondary network (Figure 8).

Variations and combinations of these systems are used, but this does not alter the basic data presented here.

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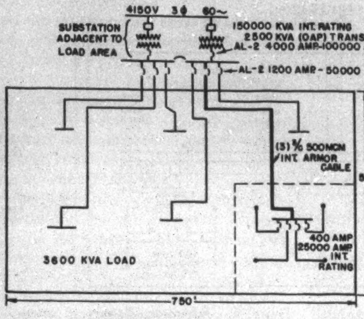

4150V 3φ 60~
SUBSTATION ADJACENT TO LOAD AREA
150000 KVA INT. RATING
2500 KVA (OAP) TRANS.
AL-2 4000 AMP-100000 AMP INT. RATING
AL-2 1200 AMP - 50000 AMP INT. RATING.
(3) ⅜ 500MCM INT. ARMOR CABLE
500'
400 AMP 25000 AMP INT. RATING
3600 KVA LOAD
750'

Figure 1. Schematic diagram of conventional distribution system

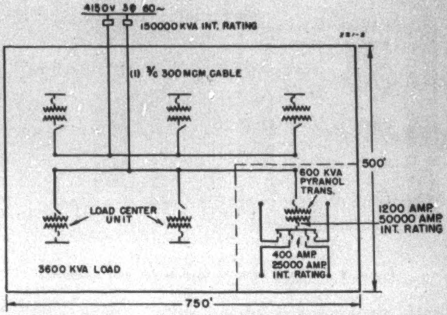


Figure 2. Schematic diagram of load-center distribution system

To simplify the text and aid in clarifying the interpretation of results, the system comparative study has been presented in two sections:

1. A comparison of the single large substation versus the distributed load-center system.
2. Comparison of the several forms of load-center distribution.

I. Single Large Substation Versus Distributed Load-Center System

In the past it has been common practice to distribute power to a relatively large factory area at utilization voltage from a single step-down substation accommodating several thousand kilovolt-amperes. This will be referred to as the conventional method, a typical circuit diagram of which is illustrated in Figure 1.

The modern load-center distribution method incorporates distributed step-down stations of small capacity (600–1,000 kva for 440-volt operation and 300–600 kva for 208Y/120- or 220-volt operation) from which low-voltage power is distributed to the immediately surrounding area as shown in Figure 2. A *load-center unit* is defined as an integrated step-down station consolidating step-down transformer, high-voltage switching unit, and low-voltage switching equipment, as typified by the illustration in Figure 3.

Application of the distributed load-center system not only allows a substantial reduction in total system investment cost, but also provides numerous other advantages.

COST

The distributed load-center system shows a lower installed cost than the conventional system because:

1. Power is transmitted directly to the utilization area at high voltage. This materially reduces cable investment cost and total I^2R losses.
2. The cost of low-voltage switching equipment is materially less than for the conventional method because of lower short-circuit currents associated with the smaller substations.

Although transformer cost per kilovolt-ampere diminishes with increasing rating, this influence is far overshadowed by the cost reductions enumerated under 1 and 2 for ratings of 600–1,000 kva at 440 volts and 300–600 kva at 220 volts.

For example, the installed cost of these two systems, based on the reference load block of 3,750 kva, would be about $71,000 for the distributed load-center system, Figure 2, compared with about $103,000 for the large single substation type Figure 1.

Reductions in secondary cable and secondary switching-equipment costs for the distributed load-center system overshadow the increase in transformer cost. The necessity of simultaneous recognition of all system parts for determining the lowest *over-all* first cost is evident.

SYSTEM PLANNING AND FINANCING

The use of small load-center substations permits electrical capacity to be added in small increments when and where needed; thus large capital layouts are not required, making it easier to finance plant extension. This eliminates the necessity for involved planning; hence, reduces engineering costs and errors. On the other hand, large substations require involved forecasting of location and magnitude of loads and high initial investment, which is not completely put to work until the anticipated load is reached. If future developments do not permit utilizing the capacity of the large substation as planned, then the initial investment is not utilized efficiently or effectively.

A spare transformer for the small load-center unit represents less idle capital than the larger spare unit for the large station.

INSTALLATION CONSIDERATIONS

The small substations compared with large ones offer many advantages from the standpoint of handling and installation. For example:

1. No expensive enclosures or foundations are required.
2. The small self-contained metal-enclosed load-center unit can be located within the working area close to the center of the particular load area being served, while the large substation must generally be located at one side of the working area because of size and weight of component parts to be handled.
3. They can be moved more easily from one location to another to cope with changes in electrical demands accompanying changes in manufacturing technique.
4. In the event of a failure, service can be restored more quickly because less time and equipment is required to move the small units. The change can usually be made with equipment and personnel normally available around the average factory.

These advantages are only fully realized with completely metal-enclosed unit-type substations as shown in Figure 2.

VOLTAGE REGULATION

Because of the shorter secondary runs in load-center distribution, voltage drop and light flicker are less. This improves the performance of motors and lamps, and hence facilitates more and better production.

SYSTEM FLEXIBILITY

Load-center distribution is ideally adapted to industrial plants, because it may be used in various circuit arrangements to meet varying degrees of service continuity required by manufacturing processes, and because small units are

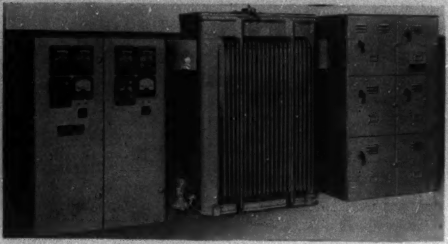


Figure 3. Typical form of load-center unit substation

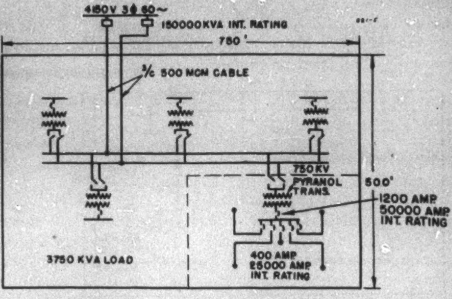


Figure 5. Schematic diagram of primary-selective-circuit arrangement

used, variation may be adapted within small factories.

This comparison indicates the load-center distribution system as being the most desirable for the average industrial plants. The single large substation design is not only more costly, but is deficient in other important respects, such as electrical operating efficiency, voltage regulation, greater initial engineering and planning, higher financing cost, and less flexibility as to future expansion or extensions.

Subsequent analysis of variations in system arrangement will, therefore, be limited to the distributed load-center design.

II. Load-Center-System Circuit Arrangements

The principal reason for considering variations in circuit arrangements is that of service reliability. The various arrangements may be classified into four groups typified by forms 1, 2, 3 and 4, which provide progressively increased service reliability and may be individually characterized as follows:

(All percentage cost figures are based on the typical example previously defined.)

Type 1—Simple Radial (*See Figure 4*)

This arrangement represents the simplest form of load-center distribution and provides a single direct electrical channel extending to each low-voltage load area. All system elements are considered to be operated at rated capacity.

The lowest possible *installed cost* is obtained due to simplicity of the arrangement and complete absence of secondary power-supply duplication, together with selection of the optimum size of load-center unit. (See appendix for further discussion.)

For cost comparison, this arrangement will be arbitrarily assigned a value of 100 per cent.

The inherent simplicity together with circuit breakers of adequate interrupting capacity results in a high order of safety. The load-center unit-substation primary disconnecting switches are for isolating a load-center unit only when completely deenergized.

All load to a particular area is always delivered by way of a single well-defined route which makes for simplicity in operation.

Voltage regulation is only slightly inferior to the best circuit arrangement considered, the difference being due to normally operating all transformers and cable at full rated capacity.

Service reliability of this arrangement is not the best, for a failure of the primary cable or its associated junctions interrupts power flow to the entire load block pending repair or installation of a temporary high-voltage circuit in place of the faulty section. A failure of a load-center transformer or its associated connections results in an interruption of power to that local area pending repair or replacement of the faulty unit. To deenergize one element for inspection or maintenance (that is, primary feeder breaker, load-center transformer or high-voltage disconnect switch) likewise demands interruption of power flow.

In the great majority of applications, a higher degree of service reliability will be desired and warranted.

Type 2—Primary Selective (*See Figure 5*)

As will be observed from the figure, this circuit arrangement is distinguished by the use of duplicate primary high-voltage feeders, either of which is capable of handling the entire load block in combination with a high-voltage transfer means at each load center.

Under normal operating conditions, approximately equal numbers of load-center unit substations are fed from each primary cable. While operating normally in this condition, operating characteristics are practically the same as the type 1.

The presence of the primary transfer means makes it possible to reestablish service to all load centers with one primary feeder deenergized.

A failure of one primary cable would interrupt service to those load centers which were being fed from this circuit. Service can be restored by changing the position of the transfer means at the respective load-center unit substations. The duration of the outage will depend on the time required to locate the system operator who is authorized to operate the transfer means and the time consumed in visiting those stations at which power interruption occurred.

For maintenance or inspection of pri-

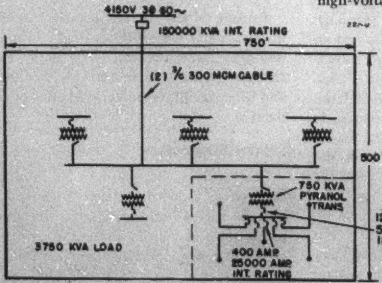


Figure 4. Schematic diagram of simple radial-circuit arrangement

mary circuit equipment, only a short interruption is occasioned since load centers are individually transferred before deenergizing the primary feeder.

It is important to note that, following reestablishment of voltage on a primary feeder, load-center units should again be returned to their normal feeder, which will account for another service interruption of short duration.

The use of a *primary loop circuit* as illustrated in Figure 6 constitutes a variation of the primary selective circuit arrangements. Unless automatic sectionalizing circuit breakers are incorporated, a primary circuit fault results in an extended interruption to the entire load, no part of which can be reenergized until the circuit has been sectionalized to exclude the faulty section. The chief advantages of the loop circuit are:

1. The ability to combine feeder and tie-circuit functions when primary power-supply stations are located on opposite sides of the plant.
2. The ability to isolate a particular load center in the event that fire or explosion should damage both primary cables at one location.
3. Primary circuit loading is inherently equalized with both primary switches at each load center closed as they normally would be.

The *installed cost* is increased due to additional primary switching equipment, high-voltage cable, and the use of primary transfer means on all load-center unit substations. The cost of this arrangement, relative to the cost of the simple radial becomes about 130 per cent.

For a primary circuit-interrupting requirement not exceeding 50,000 kva at circuit voltages below five kilovolts, metal-clad circuit-breaker transfer means, including a two-position structure with one circuit breaker, can be used without increasing the over-all cost level by more than approximately five per cent, as compared with disconnecting switches as the transfer means.

Safety is impaired when the primary transfer means is disconnect switches. Although interlocked to prevent operation except when low-voltage breakers are open, hazards nevertheless exist. The presence of a transformer turn-to-turn coil failures may allow the flow of substantial primary current irrespective of low-voltage breaker position. A major electrical fault at a transformer or its associated primary connections, which produces a primary circuit trip-out, may lead to serious consequences since normal routine procedure provides for transferring all deenergized load-center units to healthy feeders. When the faulty transformer is so transferred, the faulty circuit is *closed* on the healthy feeder by means of the transfer disconnect switch. Of lesser importance but nevertheless disturbing is the fact that a complete shutdown of both primary feeders results, and much confusion may easily exist before service is reestablished.

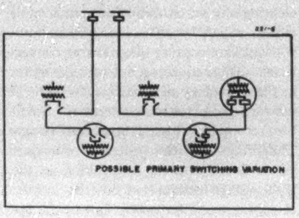


Figure 6. Schematic diagram of primary-selective-circuit arrangements with loop primary feeder

Circuit breakers adequate for high-voltage system short-circuit duty should be considered in all cases, as they eliminate the shortcomings of disconnect switches.

Service reliability is much better than that provided by this circuit arrangement except under unusual circumstances as covered under the topic of safety. It is to be expected that primary circuit failures will be far more numerous than transformer failures, many of which will be accounted for by imperfect cable tap connections or termination.

The service interruption of more or less indeterminate duration following a primary circuit outage is of course objectionable but may, in many instances, be tolerated.

The fact that both primary circuits are usually brought into close proximity at every unit substation represents a distinct hazard to service continuity.

The presence of the transfer means which must be manipulated on a number of load centers for every transfer from normal to emergency operation and again in restoring normal conditions, makes this circuit arrangement slightly more complicated to operate than the simple radial circuit arrangement.

TYPE 3—SECONDARY SELECTIVE (*See Figure 7*)

The secondary selective circuit arrangement differs from those previously described in that the load center incorporates complementary branches, each permanently associated with a particular high-voltage feeder. Transfer from normal to emergency operation is accomplished by means of a low-voltage tie breaker between the two complementary sections. This extends the duplication of supply to the load-center low-voltage bus and thus provides for continued service with either a transformer or a primary feeder out of service.

Under normal conditions the tie breaker is open and both transformer breakers are closed, in which state the system exhibits all the elements of simplicity to be found in the simple radial system.

For emergency operation, the transformer low-voltage breaker associated with the channel which has been or is to be deenergized is opened and the low-voltage tie breaker closed. Loading levels have been selected such that a transformer is subjected to not more than 125 percent rating during emergency operation.

The *installed cost* is increased over that of the primary selective arrangement largely because of reduced normal loading level on transformers. The relative cost level is between 145 and 150 per cent of the cost of the simple radial circuit. However, the reduced normal transformer loading provides additional benefits, aside from service reliability in the form of lower losses, lower transformer temperature and consequent increased life, and better voltage regulation. The cost per kva can be reduced by normally operating the transformers at a load more nearly equal to rating, depending on load curtailment to avoid excessive transformer load during emergency operation.

The same order of *safety* as associated with the simple radial circuits is retained. All transfer switching from normal to emergency conditions and vice versa is performed on full interrupting-capacity low-voltage air circuit breakers. The possibility of producing a complete primary system shutdown by transferring a fault to the healthy feeder is eliminated. The individual transformer primary disconnect switch is used only for isolating purposes and on this basis may be considered to represent only a slight hazard.

In respect to *service reliability*, this circuit arrangement excels the primary-selective circuit. Continued operation is insured with a transformer as well as a primary cable out of service. For very little increased cost, provision can be made for automatic transfer to emergency operation through electrical operation of the tie breaker, by which means restoration of voltage may be accomplished within an interval of about one to two seconds. The normal operation of transformers at considerably less than rating allows an unusual temporary load demand in any area to be met without distress.

Simplicity of operation comparable with that of the simple radial circuits is obtained. The low-voltage tie breaker between complementary bus sections represents the only feature which modifies the operating procedure.

Voltage regulation is reduced under normal operating conditions as a result of reduced transformer loading. Voltage regulation under emergency operating conditions is only slightly inferior to that of the simple radial circuits as a result of transformer operation of 125 per cent rating.

TYPE 4—SECONDARY NETWORK (*See Figure 8*)

The secondary network system is distinguished from others in that continuously connected duplicate sources are provided to each low-voltage load-center bus. The prime source is represented by a transformer tie to the primary system while low-voltage tie circuits interconnected with one or more other prime sources constitute the emergency source. Two or more primary feeders are used, and the emergency source for one load center is derived from prime sources associated with a different primary feeder.

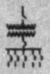
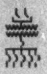
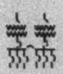
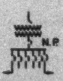
Provision is made for automatically clearing any particular prime source or tie circuit which becomes involved in fault without interrupting other source connections. This requires a network protector in the secondary of each transformer. A network protector is an automatic circuit breaker equipped with directional relays. The relays trip the protector whenever power flows from the low-voltage to the high-voltage terminals of the transformer. It does not trip for power flow in the opposite direction. Automatic reclosing is provided.

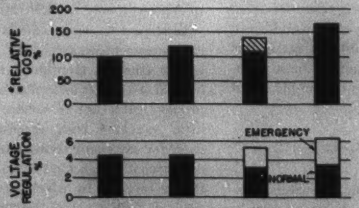
The required reserve capacity in primary cables and transformers is not greatly different than that for the secondary selective circuits. Loss of voltage on one primary feeder immediately results in automatic transfer of the load previously carried by that feeder to adjacent load centers by way of the interconnecting low-voltage tie circuits. Reserve transformer capacity is needed to avoid transformer loading levels in excess of 125 per cent of rating. Two primary feeders, to which the load centers are alternatively connected, would require that the normal load level per transformer be held to 63 per cent of rating. The required transformer capacity may be reduced by about 30 per cent by the use of three or more primary feeders. This can also be accomplished in two-feeder systems by the introduction of primary transfer switches in combination with suitable means for insuring prompt transfer of load-center units to healthy feeders following loss of voltage on one feeder. Transfer devices offer no economic advantage over three-feeder systems, hence are not considered because of the hazard of transfer switches.

It will be immediately apparent that the introduction of permanently connected emergency tie circuits will increase the low-voltage short-circuit current level unless the size of load-center units is simultaneously reduced. Where a number of load-center units are interconnected on the low-voltage side, careful design and specification of tie circuit impedance will be necessary to realize the most favorable economic balance. It will quite often be necessary to insert reactors in the tie circuits to obtain the desired results. The same care in system design is required in incorporating one or more additional load-center units in the network at a future date as dictated by future load conditions.

The *installed cost* of the secondary network referred to the simple radial type will be in the range of 175 to 200 per cent.

Table I. Relative Performance Characteristics, Load-Center Distribution System Design

	NO 1 SIMPLE RADIAL (FIG 4)	NO. 2 PRIMARY SELECTIVE (FIG 5)	NO. 3 SECONDARY SELECTIVE (FIG 7)	NO. 4 NETWORK (FIG 8)
LOAD CENTER UNIT DETAIL				 N.P.
TRANSF. L.V. CIR BRKER	AL-2 1200A (50000 IC)	→	→	1200A NET PROT.
FEEDER CIR BRKER RATING	AE1B 400A (25000 IC)	→	→	AL-2 400A (50000 IC)
TOTAL NO. FEEDER CIR. BRKERS	25	25	24	24
LOAD CENTER UNITS NO.	5	5	4	6
UNIT TRANSF KVA	750	750	750	750
TOTAL TRANSF KVA	3750	3750	6000	4500
FIRM LOAD CAPACITY	—	*3750	*3750	*3750



INTANGIBLE CHARACTERISTIC (A-BEST)	NO 1	NO. 2	NO. 3	NO. 4
SAFETY	A	C	A	B
SERVICE RELIABILITY	D	C	B	A
EASE OF OPERATION	A	B	B	B
MAINTENANCE	A	C	B	D

*For high-voltage feeder outage only.

**Based on load area defined in text.

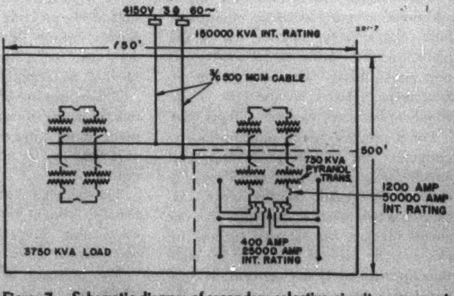


Figure 7. Schematic diagram of secondary-selective-circuit arrangement

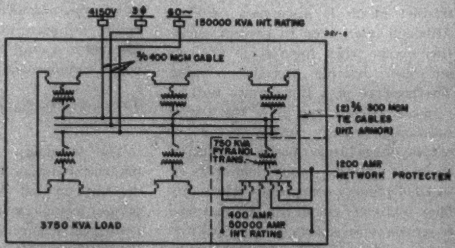

4150V 3φ 60~
150000 KVA INT. RATING
3/c 400 MCM CABLE
(2) 3/c 300 MCM TIE CABLES (INT. ARMOR)
750 KVA PYRANOL TRANS.
1200 AMP NETWORK PROTECTER
400 AMP 50000 AMP INT. RATING
3750 KVA LOAD

Figure 8. Schematic diagram of secondary network

The increased cost is the result of the introduction of low-voltage tie circuits and the more costly low-voltage switching equipment demanded by increased short-circuit current level.

With respect to *safety*, the secondary network, with low-voltage circuit breakers of adequate interrupting capacity, is comparable with the secondary selective, and the simple radial types. The fact that more than one source circuit must be opened to deenergize a particular load-center low-voltage bus represents some additional hazard, unless highly trained personnel is available.

The increased low-voltage short-circuit level to be expected with the secondary network is opposed to the interests of safety. Higher short-circuit current levels mean more violent disruptive effects at the point of fault. It may also jeopardize the safety of nearby branch circuit-switching units and individual load controllers unless the system is carefully checked and designed to avoid such a possibility.

Service reliability is of the highest order to the load-center low-voltage bus. A primary feeder or transformer unit may be deenergized without incurring interruption of service to any load center. Electrical faults in primary feeders or transformers will be automatically removed without service interruption except for a momentary voltage depression during the time the faulty element is being severed from the system.

Operating simplicity is sacrificed in the secondary network. In general any system incorporating multiple paralleled branches introduces operating complications. Should primary feeders originate from different high-voltage bus sections, as they should preferably do, voltage or phase-angle differences between these two sources will cause circulating currents which increase the loading on some load-center transformers and may create many perplexing problems for the operating departments. To deenergize a particular load-center low-voltage bus requires that two or more source circuits be opened. The maintenance and adjustment of network protectors with their associated network relays require greater skill than do conventional air circuit breakers.

Voltage regulation is good but not significantly superior. The normal load regulation may be either above or below the secondary selective circuit, depending on the particular form being considered. The regulation under emergency operation is likely to be inferior to the secondary selective form due to voltage drop in the low-voltage tie circuits.

It excels in the ability to accept heavy motor starting currents without light flicker which is directly the result of higher short-circuit current levels. This feature will be of value, however, only if motor starting currents approach that value which will produce light flicker. For instance, across-the-line starting of a 25-horsepower motor on a 440-volt system having a short-circuit current level of 25,000 amperes will result in a voltage drop of only one per cent. Since this would be judged to represent no noticeable flicker effect even with incandescent lighting, an increase in short-circuit current level could not be credited with improving light flicker. However, when larger motors are to be started on the 480-volt system, the network acquires some additional advantage.

Summary of Comparative Performance

Comparative installed-cost and electrical-performance characteristics of the various forms of distributed load-center systems are condensed in Table I. Qualities which are incapable of numerical evaluation are rated in terms of letters, *A* representing the best, *B* the next best, and so on.

The system cost comparisons are based on the representative load area expressly defined in the early part of this paper. While deviations from this basic load condition will influence the specific values to some degree, all systems will be similarly affected.

In every case, air circuit breakers of adequate interrupting capacity have been used for all low-voltage switching operations. Cascade arrangements[5] are used where it permits savings on breaker cost. All systems have been designed to provide individual control of the same number of secondary feeder circuits. An average unit feeder capacity of 150 kva (about 200 amperes at 440 volts) has been selected as representing a reasonable value commensurate with operating flexibility and ease of installation.

The selection of load-center unit ratings has been made with optimum installed cost as the objective.

Application of Limiters

For the purpose of diminishing the installed first cost, particularly in the case of the secondary network system, the substitution of limiters for air circuit breakers has been proposed.

To realize the service reliability of which the system is capable, of course, demands that the fault-current interrupting ability be unquestionably established under service conditions.

The application of limiters without proper isolating switches seriously impairs safety and operating flexibility. To deenergize any part thereof requires either that the entire network be deenergized or the particular section be isolated by literally cutting the energized conductors. The former would nullify the service continuity rating which the network systems claims as its chief advantage. The latter constitutes a serious hazard to operating personnel.

All repairs, additions or modifications made after the system is initially energized must be worked "hot". Although such practice is quite common in the utility industry, its successful application depends on a maintenance crew unusually skilled in handling electrical circuits and kept in practice by continual experience. The usual industrial plant would be unlikely to maintain a competent group of such skilled men and would afford insufficient practice to maintain proficiency.

The usual limiters incorporate no positive operation indicator. Tie circuits may have been divorced from the system as a result of internal fault without warning to the operating department and thus constitute a hazard to service continuity under emergency operating conditions.

Selection of Circuit Arrangement

The problem of selecting that circuit arrangement which will satisfy application requirements becomes primarily one of balancing service reliability against installed cost. It should, however, be borne in mind that a system, theoretically not perfect, but with the best possible type of equipment, is better than a system more perfect on paper, but with second-rate apparatus, such as a fusible element instead of a breaker.

Service reliability requirements will vary widely for various service conditions. The requirements for warehouses and storerooms will be low, although the ability to restore excitation to the more important circuits is obviously advantageous. General manufacturing plants operating on a piece-rate basis have observed that electrical-service outages of as much as twenty or thirty minutes rarely result in diminished output for that day. A few industrial processes are seriously influenced by service interruption for any significant interval, such as wire enameling and glass working.

In these more exacting applications, the secondary network system may be justified, although even an allowable outage interval of two or three seconds will permit application of the secondary selective circuits with automatic transfer, at considerably less cost.

For all but the most exacting requirements, the secondary-selective-circuit arrangement should find general if not universal application in industry in view of these outstanding merits.

1. MODERATE INSTALLED COST

Based on maintaining full system load during emergency operation, the cost is moderately greater than that of the minimum tolerable system arrangement. The increased cost above the simple radial system is largely invested in active material (primary cables and transformers) which return benefits in the form of improved voltage regulation, reduced operating temperature, lower losses, and the ability to meet unusual temporary peak load demands without distress.

For areas which allow a lower order of service reliability, first cost can be reduced by curtailing load during emergency operation. A 40 per cent load reduction during emergency operation allows system first cost to be reduced to within about 10 per cent of the simple radial circuit. No change in load-center size or design is needed, the result being accomplished merely by assigning a larger normal load block to the particular load center.

2. HIGH ORDER OF SAFETY

All secondary switching operations affecting service are performed with low-voltage air circuit breakers of adequate interrupting capacity. The disconnecting switches on the primary of the transformers need only be operated when completely deenergized.

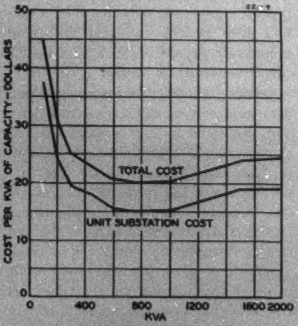


Figure 9. Cost of load-center distribution as a function of transformer size

480 volts

3. GOOD SERVICE RELIABILITY

Each secondary feeder circuit has access to two low-voltage sources derived from different primary feeders. Automatic transfer may be readily incorporated to limit service interruption to an interval of one or two seconds.

4. SIMPLE TO OPERATE

The operating simplicity of the simple radial system is obtained. There is no question as to the proper switching operation required to meet a particular situation. Each duplex load-center unit is self-contained and independent of others.

5. VOLTAGE REGULATION IS OF THE BEST

The normal voltage regulation is entirely comparable with that of any other system, although the difference between any of the distributed load-center systems is actually not significant.

Appendix

Optimum Size of Load-Center Unit

The most economical size of unit substation is determined principally by the cost of the unit substation itself. This point is illustrated in Figure 9, which shows the cost per kilovolt-ampere of the small capacity of a radial load-center distribution system and the cost per kilovolt-ampere of the load-center unit.

The smaller load-center units are more expensive, because small equipment is inherently more expensive per kilovolt-ampere. The larger load-center units become increasingly expensive because of the large switchgear required to handle the high short-circuit current accompanying larger transformer banks. The optimum size of transformers lies between these two limits and for 480-volt secondaries 600-, 750-, and 1,000-kva units are most economical.

The shape of these curves of Figure 9 is accentuated by cable costs. For the smaller units more primary cable but less secondary cable is required. For the larger units less primary cable is required, but there is a very material increase in the amount of secondary cable necessary to transmit the power over the larger load area. This point is illustrated in Figure 10.

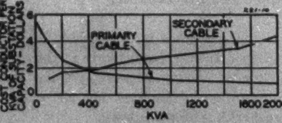


Figure 10. Cable cost as a function of transformer size

Load density affects the over-all system costs, principally because of the effect on cable cost. The total installed cost per kilovolt-ampere of load-center distribution system for load densities 2 volt-amperes to 20 volt-amperes per square foot inclusive are shown in Figure 11. The range of load densities studied should cover most average factory installations. It is interesting to note that, regardless of load densities, the shape of the curves is the same, and the optimum size of units is not affected.

The kilowatt losses per kilovolt-ampere capacity is substantially constant over the range of units from 100 to 1,500 kva.

The above curves have all been for 480-volt secondaries. When lower-voltage secondaries are used, the cost per kilovolt-ampere of the system is higher, because of the great amount of secondary cable required and because of the increased cost of secondary switchgear. This results in most economical sizes of transformers for 240-volt or 120/208-volt circuits, being 300 to 600 kva.

Figure 12 shows the comparative cost of 120/208-volt load-center radial system compared with 480-volt system.

Voltage Regulation and Short-Circuit Current Level

The normal voltage regulation of a distribution system will be controlled primarily by transformer regulation and voltage drop in low-voltage circuits.

Transformer regulation will be in the order of one per cent at 1.0 power factor and three to four per cent at 0.8 power factor at rated current and correspondingly less at lower currents.

The voltage regulation in low-voltage circuits is a function of conductor configuration, current loading and length of run. Using three-conductor or three single-conductor cables in conduit, in sizes ranging from 1/0 to 500,000 at current levels approaching the thermal rating, the voltage regulation, on the basis of 440 volt, three-phase, three or four-wire service, will be in the order of one-half to three-fourths per

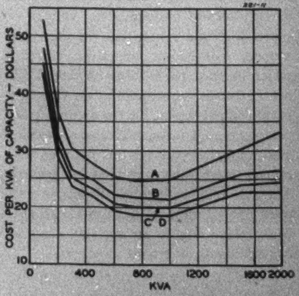


Figure 11. Load-center distribution system costs as affected by load density

480 volts

Curve *A*—2 volt-amperes
Curve *B*—5 volt-amperes
Curve *C*—10 volt-amperes
Curve *D*—20 volt-amperes

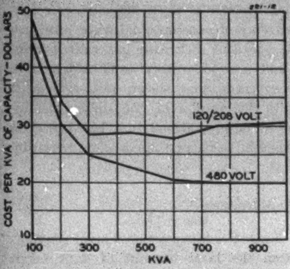


Figure 12. Comparative costs of 120/208-volt and 480-volt load-center distribution systems

cent per hundred feet of transmission, and will be more or less independent of power factor in the range of 0.7 to 0.9 lagging. At lower operating voltages, the per cent voltage regulation will be correspondingly increased.

Separation of conductors increases the reactive voltage drop and increases the voltage regulation by increasingly greater amount as the power factor deviates from unity in the lagging direction.

Incremental voltage or flicker-influence factor goes hand in hand with short-circuit level. The greater the short-circuit current the lower will be the abrupt voltage change resulting from a given low-power-factor abruptly applied load.

However, increased short-circuit current level means increased low-voltage switching-equipment expense, more violent disturbance associated with an electrical fault, and restricted application of conventional motor starters. It is therefore advisable to design for the lowest short-circuit current level commensurate with best overall economy and freedom from light flicker. No benefit can be claimed, as far as light flicker is concerned, for increasing the short-circuit current above that required for freedom from flicker.

The magnitude of abrupt voltage dip which can be permitted if the frequency of the occurrence is of the order of a few per hour is about two per cent for incandescent and about four per cent for fluorescent lighting.

Table II shows the required short-circuit current level to permit full-voltage starting of general-purpose induction motors for two per cent and four per cent voltage dip respectively on a 440-volt, three-phase system.

Table II. Required Short-Circuit Current Level for Limited Voltage Dip With Across-the-Line Motor Starting

Motor Rating (Horsepower)	2% Dip (Amperes)	4% Dip (Amperes)
15	7,500	3,750
30	15,000	7,500
50	25,000	12,500
100	50,000	25,000

Effect of Load-Density Variation

Variation in load density influences the mean length of run (*MLR*) between the load-center bus and the utilization machine. The *MLR* directly affects the investment cost in low-voltage distributing circuits (about two to four dollars per kilovolt-ampere per 100 foot at 440 volts) and the voltage regulation (about one-half to three-fourths per cent per 100 foot at 440 volts with closely spaced conductors).

The area associated with a given load center will be directly proportional to load-center rating and inversely proportional to load density. It follows that the *MLR* will vary in like manner except as the square root of load-center rating and load density, since the length of run is governed by the lineal dimension of the sides. Expressed mathematically, this becomes

$$MLR = K\sqrt{\frac{L.C.\ \text{rating}}{\text{load density}}}$$

assuming, of course, that the shape of the load area remains unchanged.

Low-voltage circuit runs generally follow a rectangular course coinciding with the building form rather than a direct diagonal path to the subdistribution point which increases the length of run by 40 per cent for square load areas.

To enable rapid evaluation of low-voltage *MLR*, the attached Figure 13 has been prepared, giving the horizontal *MLR* as a function of load density for various load-center ratings, based on rectangular configuration. To this must be added the vertical lengths almost invariably involved. For floor or underfloor installation, a 10-foot allowance (5 feet at each end) is reasonable. For overhead installation, a 15- to 30-foot allowance would generally be expected.

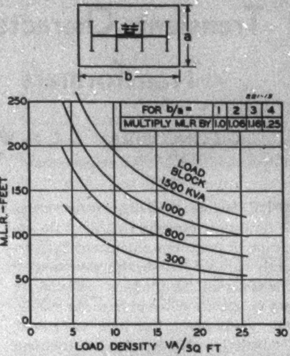


Figure 13. Mean length of secondary run versus load density

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Transient Characteristics of Current Transformers During Faults

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THE factors involving current-transformer performance that affect the operation of protective equipment have recently received considerable attention.[1–3] The fact that the transient performance may be greatly different from the steady-state performance has been recognized, but little quantitative data on transient performance have been published. Data from oscillograph records of tests and from approximate analytical methods have definitely indicated the magnitudes of the expected steady-state errors and at the same time conveyed the thought that the transient errors would be very much larger. This paper presents some of the results of a study of current-transformer transient performance that has been made on the differential analyzer with the effects of transformer saturation more accurately considered.

While the information presented herewith is neither a complete coverage of the subject nor even a complete summary of what has been done to date by the authors, it has seemed desirable to publish it now for the general benefit of the interested engineers. More specifically the data presented apply to current transformers such as might be encountered in bus-differential protection. The magnitude of expected error under various conditions of application was studied for a wide range of transformers of the conventional bar-type construction and of the bushing type, but it is possible to present only a small part of these results here because of space limitations. Most of the data presented here on the bushing-type transformer was for a transformer physically larger than would be normally used, in order that the data would show what improvement can be affected through increase in size.

Certain broad assumptions have been made regarding the distribution of fault current between circuits, the degree of similarity of the various transformers, and so on, since all combinations of possible operating conditions cannot be considered in a study of this type. However, the data will be useful as a basis for judging the expected performance of existing and contemplated installations, particularly since the effects of variations of such important factors as the magnitudes of fault current, size of transformer, number of transformers, and time constant of the d-c component of fault current have been studied. In addition to the factors involving the current, the data have been referred to a base involving a factor which is called the transformer size constant. This size constant depends upon the physical construction of the transformer and the total secondary resistance both of the winding and of the burden. With this information the data already presented can be translated to apply to other transformers if the physical dimensions and the secondary loading are known.

The results were recorded as reproductions of the error current in the differential-relay circuit versus time during periods of large through-fault currents, and a portion of these results is summarized here as a function of the governing factors mentioned above, with respect to the application possibilities of instantaneous overcurrent and of time-delay overcurrent relays. For other types of relays, such as those having harmonic restraint,[4] d-c restraint, percentage differential restraint, and so on, it will probably be necessary to consider each application on its own merits, at least until a time when the response of these relays to transient currents of nonsinusoidal wave form can be specified in a more generalized form. Field and laboratory tests which have been made have shown good agreement with the results of the differential-analyzer study, although it has not been possible to cover the complete range by test.

Conclusions

From the data presented here, as well as from the large amount of data taken which could not be included, the following conclusions may be drawn:

1. The concept of transformer size constant has been introduced. The transient performance of two transformers having the same size constants and saturation characteristics will be practically identical when the primary current, in ampere turns per unit of core length, is the same for both. Of two transformers having the same size constant the one having the longer magnetic circuit will give better performance for the same value of primary current.

2. Data have been obtained to show the calculated differential-circuit error currents, that appear as a result of transformer saturation, for a wide range of magnitudes of through-fault current, transformer size constant, d-c time constant of the primary circuit, and number of transformers in the group.

3. When conventional current transformers are used, the magnitude of differential-error current with faults of ten or more times rated current can be from 30 per cent to 75 per cent or larger, depending on the size, even at a high-voltage bus where the d-c time constant of the fault current may be only two or three cycles.

4. From a study of the differential-error current curves obtained, it is apparent that time-delay settings of only three or four cycles will not insure proper operation, when simple time-delay relays are used for bus-differential protection; very much greater delays may be necessary.

5. The differential-error currents are particularly severe at a bus where the fault current has a very long d-c time constant. For these applications it is not practical to design conventional current transformers large enough to insure that they will not saturate at the larger values of fault current. Therefore, there is a definite field for the new air-gap current transformer recently introduced.[5]

6. The form and magnitude of the dif-

Table I

Transformer	Size Constant Turns^2 Inch/Ohms	Type	Rating		Core Area Sq In.	Mean Length of Core, In.	Secondary Turns	Secondary Resistance, Ohms
			Amperes	Ampere-Turns per Inch Core Length				
A	16,700	Bar	4,000/5	160	1.30	25.0	800	1.99*
B	50,000	Bushing	4,000/5	107.5	9.90	37.3	800	3.40*

* Including 1 ohm lead resistance.

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This paper is the result of a co-operative effort of the authors and S. B. Crary, L. F. Kennedy, C. D. Hayward, A. T. Sinks, G. Camilli, and F. J. Maginniss of the General Electric Company, and of Dr. Irven Travis of the University of Pennsylvania. The differential analyzer at the Moore School of Electrical Engineering, University of Pennsylvania, was used to solve the equations.

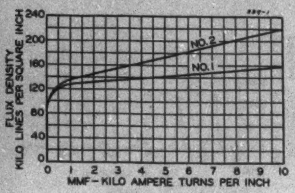

Figure 1. Saturation characteristic of the mutual flux path

ferential-error current depends greatly on the number of transformers in the group.

Analysis of Current-Transformer Performance

The performance of a current transformer can be determined from the voltage equation of its secondary circuit

$$10^{-8}\frac{d\psi}{dt}=i_2r_2+L_2\frac{di_2}{dt} \tag{1}$$

where

$\psi=N_2\phi$ is the flux linkages of the secondary due to the mutual flux
N_2 = the number of secondary turns
ϕ = the number of flux lines mutual to both the primary and secondary windings
i_2 = the secondary current in amperes
r_2 = resistance of the secondary circuit, including that of the burden, in ohms
L_2 = secondary leakage inductance plus the inductance of the burden in henrys
t = time in seconds

Equation 1 is integrated to give

$$\frac{\phi}{10^8}=\int\frac{r_2}{N_2}\,i_2\,dt+\frac{L_2}{N_2}\,i_2+\frac{\phi_0}{10^8} \tag{2}$$

where the constant of integration is proportional to the residual flux ϕ_0.

Another relation is that the magnetomotive force acting on the core is proportional to the primary ampere turns less the secondary ampere turns, or, for a transformer having only one turn in the primary

$$N_2i_2=i_1-\frac{hl}{0.4\pi} \tag{3}$$

where

i_1 = primary current in amperes
$h/0.4\pi$ = magnetomotive force acting on the core in ampere turns per inch of core length
l = mean length of core in inches

The flux mutual to the primary and secondary windings can be written as

$$\phi=A\beta \tag{4}$$

where

A = area of core in square inches
β = flux density in the core in lines per square inch

Upon substituting the expressions for the secondary current i_2 and the mutual flux ϕ of equations 3 and 4 into equation 2, there is obtained a general equation for determining the performance of a single transformer, including the effects of its burden, when the primary current is known.

$$\frac{A\beta}{10^8}=\int\frac{r_2}{N_2^2}\left(i_1-\frac{hl}{0.4\pi}\right)dt+\frac{L_2}{N_2^2}\left(i_1-\frac{hl}{0.4\pi}\right)+\frac{A\beta_0}{10^8} \tag{5}$$

Dividing through by the factor r_2l/N_2^2, there is obtained a more convenient form,

$$C\beta=10^8\int\left(\frac{i_1}{l}-\frac{h}{0.4\pi}\right)dt+10^8\frac{L_2}{r_2}\left(\frac{i_1}{l}-\frac{h}{0.4\pi}\right)+C\beta_0 \tag{6}$$

where the factor, $C=AN^2/r_2l$ turns² inch ÷ ohms, is defined as the transformer size constant.

If the secondaries of current transformers applied to bus differential protection are connected in parallel, through a differential-relay circuit of negligible impedance, the above equations can be used to determine the performance of each transformer separately. Furthermore, previous work has shown that the transformer errors due to the residual flux and to the flux associated with L_2 are small compared with the errors caused by the flux associated with R_2. For a through fault at a bus of $n+1$ feeders and transformers, transformer 1 carries total fault current and in view of the above assumptions its performance is given by:

$$C\beta_1=10^8\int\left(\frac{i_1}{l}-\frac{h_1}{0.4\pi}\right)dt \tag{7}$$

It is further assumed that all transformers have identical size constants and saturation characteristics and that the fault current is equally divided between the other n transformers. The equation for each of the n transformers is

$$C\beta_n=10^8\int\left(\frac{i_1}{nl}-\frac{h_n}{0.4\pi}\right)dt \tag{8}$$

The current through the differential-relay circuit is equal to the secondary current of transformer 1 less the sum of the secondary currents of the other n transformers. Referred to the primary this error current is:

$$\frac{i}{l}=\left(\frac{i_1}{l}-\frac{h_1}{0.4\pi}\right)-n\left(\frac{i_1}{nl}-\frac{h_n}{0.4\pi}\right)=\left(\frac{nh_n}{0.4\pi}-\frac{h_1}{0.4\pi}\right) \text{ amperes per inch of core length} \tag{9}$$

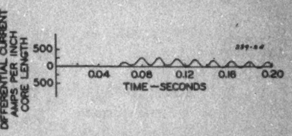

Figure 2a. Symmetrical component of fault current equal to 269 rms amperes per inch core length

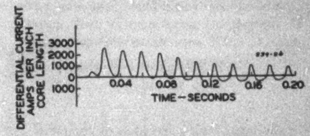

Figure 2b. Symmetrical component of fault current equal to 988 rms amperes per inch core length

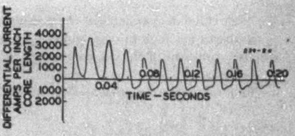

Figure 2c. Symmetrical component of fault current equal to 1,438 rms amperes per inch core length

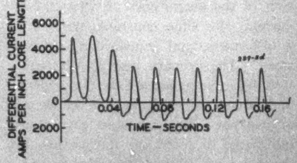

Figure 2d. Symmetrical component of fault current equal to 1,975 rms amperes per inch core length

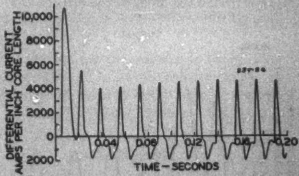

Figure 2e. Symmetrical component of fault current equal to 3,950 rms amperes per inch core length

Figure 2. Error current in the differential circuit as a function of the magnitude of through-fault current

Fault current completely offset. One transformer versus five. Saturation curve 1

C=16,700 turns² inch/ohms
T=0.265 second

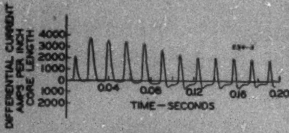

Figure 3. Differential-error current for a through-fault current having a d-c component one-half the crest value of the a-c component

One transformer versus five. Saturation curve 1. Symmetrical component of fault current equal to 1,975 amperes per inch core length

$C = 16{,}700$ turns^2 inch/ohms
$T = 0.265$ second

The total fault current is of the form

$$\frac{i_1}{l} = \frac{I_1}{l}[(\cos\alpha)\epsilon^{-t/T} - \cos(\omega t + \alpha)] \qquad (10)$$

where

I_1/l = crest value of symmetrical component, amperes per inch of core length
T = time constant of the d-c component in seconds
α = determines the magnitude of the initial offset
$\omega = 2\pi f$

Equations 7–10 were solved on the University of Pennsylvania's differential analyzer[6,7] to determine the error current in the differential-relay circuit as a function of the magnitudes of through-fault current, d-c time constant, number of transformers, and transformer size constant. The primary current i_1/l (equation 10), was generated by the machine and formed one component of the secondary current. This secondary current was integrated to determine the mutual flux density β according to equation 8, and β in turn moved an index in the β direction of the saturation curve as plotted on an analyzer input table. An operator manually controlled the index in the direction of magnetizing force H so as to keep the index on the saturation curve and, at the same time, move the exciting current component of the secondary current in proportion. The primary and exciting currents were added to form the integrand of equation 8. Two saturation characteristics were used as shown on Figure 1. Curve 1 is for silicon steel core material and was used for most of the cases considered. Curve 2 is for a transformer in which air forms a considerable part of the mutual core area in parallel with the iron. These two curves cover an extreme range of saturation characteristics for conventional current transformers. For the very high degrees of saturation which cause the large errors observed, the effects of hysteresis are negligible, and were not considered in this study. Other studies of similar transient circuit performance with iron saturation which have been made have also indicated that consideration of hysteresis is not necessary to explain the fundamental phenomena.

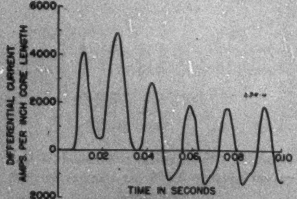

Figure 4. Differential-error current for transformers having saturation characteristic 2

Through-fault current, completely offset. Symmetrical component of fault current equal to 1,975 amperes per inch core length. One transformer versus five.

$C = 16{,}700$ turns^2 inch/ohms
$T = 0.265$ second

Results

The differential-relay error currents have been determined for a wide range of through-fault currents, transformer size, and d-c time constant. One set of results shows the error currents for the case in which the total through-fault current in one transformer is equally divided between two other transformers. The differential analyzer was set up so that the results for the case of one transformer versus three, one versus five, and one versus an infinite number, were also obtained at the same time. The data obtained for the case of one transformer versus an infinite number show in addition to the differential current which might flow in this case the absolute error current of the transformer under study. The constants for the bar-type current transformer, and for the bushing-type transformer, studied are tabulated in Table I. Data have also been obtained for very large and very small transformers having size constants $C = 200{,}000$ and $C = 4{,}170$, respectively.

The following sections describe that part of the data which is presented in this paper. The effects of variations of such important factors as the magnitudes of fault current, the d-c time constant, the number of transformers, and the transformer size constant, are shown.

MAGNITUDE OF FAULT CURRENT

Figure 2 shows the error currents (referred to the primary) as obtained with transformer A (Table I). The total through fault current in one transformer (which will be designated as transformer 1) is equally divided between five other transformers. The fault current is completely offset and has a relatively long d-c time constant of $T = 0.265$ second. The magnitudes of fault currents are given in terms of the rms value of its symmetrical

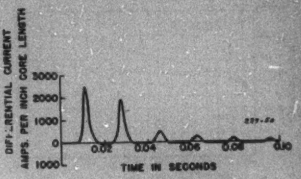

Figure 5a. $T = 0.0106$ **second**

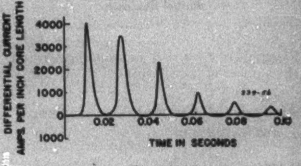

Figure 5b. $T = 0.0212$ **second**

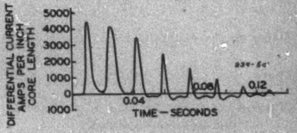

Figure 5c. $T = 0.0424$ **second**

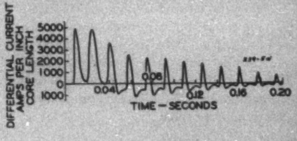

Figure 5d. $T = 0.0848$ **second**

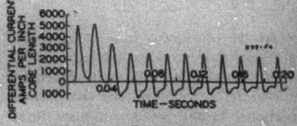

Figure 5e. $T = 0.1696$ **second**

Figure 5. Differential-error current as affected by the magnitude of the time constant of the d-c component of fault current

Through-fault current, completely offset. Saturation curve 1. Symmetrical component of fault current equal to 1,975 amperes per inch core length. One transformer versus five

$C = 16{,}700$ turns^2 inch/ohms

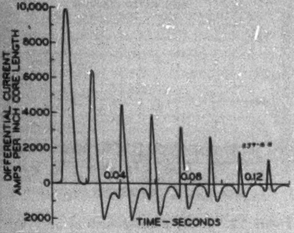

Figure 6a. $C = 16{,}700$ turns^2 inch/ohms

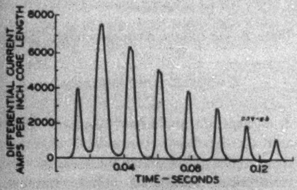

Figure 6b. $C = 50{,}000$ turns^2 inch/ohms

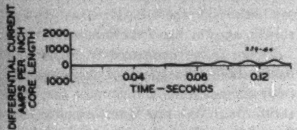

Figure 6c. $C = 200{,}000$ turns^2 inch/ohms

Figure 6. Effect of transformer size constant

Through-fault current, completely offset. Saturation curve 1. Symmetrical component of fault current equal to 3,950 amperes per inch core length. One transformer versus five

$T = 0.0424$ second

a-c component. This value multiplied by $2 \times \sqrt{2}$ is equal to the maximum instantaneous fault current for a completely offset wave.

For a small fault current having a symmetrical component of 269 rms amperes per inch core length (Figure 2a) the differential-error current is proportional to the saturation ampere turns of transformer 1, since the other transformers do not saturate when subjected to only one fifth of this value of total fault current. It is seen that several cycles are required for the d-c component of core flux to build up to the point where the saturation is appreciable. The error current has a large d-c component and the a-c component is primarily of fundamental frequency.

The other curves of Figure 2 show the effects of larger fault currents. With these larger currents all the transformers saturate, and the differential-error current is equal to the saturation ampere turns of transformer 1 less the sum of the saturation ampere turns of the other five transformers. As the magnitude of fault current is increased, the d-c component of core flux reaches a higher value in less time. That is, all the transformers lose their ability to transform the d-c component of fault current in a shorter time. This is demonstrated by the curves of Figure 2 in that the d-c component of differential-error current decays at a greater rate for the larger values of fault current. The wave form of a current through the differential relay becomes more distorted due to the greater degree of saturation associated with the larger fault current.

The maximum possible error, equal to the total fault current, occurs when the accuracy of transformer 1 collapses, and the others transform perfectly. Even with the large fault current of Figure 2e the maximum possible error is almost attained during the first cycle, because transformer 1 saturates at a much greater rate than the others, and its accuracy is destroyed before the saturation in the other transformers becomes appreciable. Another interesting fact is that the differential current is of relatively good wave form although completely or nearly completely offset during this period, but that the wave form becomes greatly distorted after the other current transformers saturate.

SMALLER D-C TIME CONSTANTS

Error currents obtained with completely offset fault currents having smaller d-c time constants are given on Figure 5. The transformer size constant, $C = 16{,}700$ turns^2 inch/ohms, is the same as used before. Maximum instantaneous fault current, neglecting the decay of d-c component in the first half-cycle, is equal to 5,590 amperes per inch core length. Even with the very short time constant of $T = 0.0106$ second, the maximum error is approximately 90 per cent of the maximum a-c component of fault current. The error current does disappear at a faster rate for the smaller d-c time constants. As a matter of interest, the value of rms symmetrical component of fault current is 12.4 times rated for the particular transformer listed in Table I as representative of this size constant. However, another transformer of identical size constant might have a different rating.

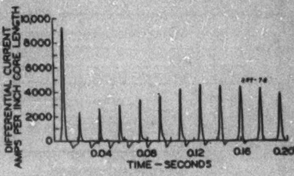

Figure 7a. One transformer versus two

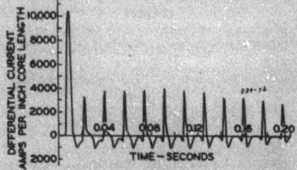

Figure 7b. One transformer versus three

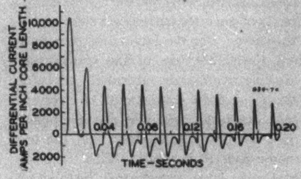

Figure 7c. One transformer versus five

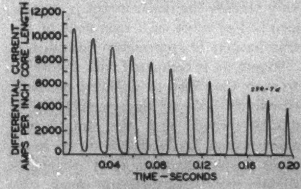

Figure 7d. One transformer versus an infinite number

Figure 7. Differential-error current as affected by the number of transformers carrying fault current

Through-fault current, completely offset. Saturation curve 1. Symmetrical component of fault current equal to 3,950 amperes per inch core length

$C = 16{,}700$ turns^2 inch/ohms
$T = 0.0848$ second

EFFECT OF TRANSFORMER SIZE CONSTANT

The curves of Figure 6 give an interesting relation, showing the comparative results for three installations operating under similar conditions but having transformers of different size constants. Although the assumed fault current is the same in amperes per inch core length for all three cases it may not be the same in actual amperes or in times rated current. For the installation having transformers of smaller size constant, $C = 16{,}700$, all six transformers become highly saturated. Therefore, the d-c component of error current decays quite rapidly, and then the harmonic content of the a-c component

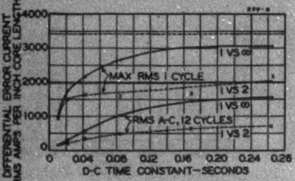

Figure 8. Summary curve showing the effect of variations in the magnitude of the d-c time constant

Through-fault current, completely offset. Saturation curve 1. Symmetrical component of fault current equal to 1,975 amperes per inch core length

$C=16{,}700$ turns^2 inch/ohms

becomes large. The maximum instantaneous fault current is large for the first cycle or two, but the harmonic components of a-c error current are small during this time. For the larger size transformers, $C=50{,}000$, five of the transformers are only moderately saturated. As before the a-c and d-c components of error current are large, but the d-c component does not decay so rapidly, and the harmonic content is small. With the hypothetically large transformer installation, $C=200{,}000$, the error current is small only because the fault current is of sufficiently low value such that none of the transformers is appreciably saturated.

Figure 6a is compared with Figure 5c to show the effect of doubling the fault current when the d-c time constant is $T=0.0424$ second, and the transformer size constant is $C=16{,}700$ turns^2 inch/ohms. The maximum error current is slightly more than doubled. Again, the higher degree of saturation obtained in the other five transformers with the larger fault current causes the d-c component of error current to decay more rapidly, and the harmonic content becomes greater.

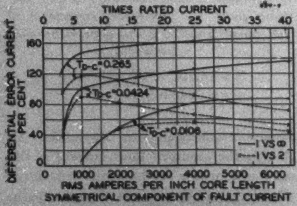

Figure 9. Maximum rms error current over one-cycle interval as a function of the magnitude of fault current and of the d-c time constant

Through-fault current, completely offset. Saturation curve 1

$C=16{,}700$ turns^2 inch/ohms

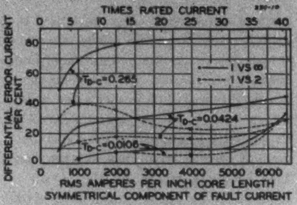

Figure 10. Rms a-c component of error current over 12-cycle interval as a function of the magnitude of fault current and of the d-c time constant

Through-fault current, completely offset. Saturation curve 1

$C=16{,}700$ turns^2 inch/ohms

Figure 13 shows the maximum differential-error currents (rms for one cycle) for a range of fault current and transformer size. The effect of variation in the saturation characteristic is also shown for one size (Curves C and C'). All of the error currents are large except for the largest transformer size with rather small fault currents.

Different Numbers of Transformers

Most of the data presented above were for a condition of one transformer versus five. Figure 7 shows the error currents for one transformer versus two, one versus three, one versus five, and one versus an infinite number. The results are all for the transformer size constant $C=16{,}700$ and a reasonably small d-c time constant of $T=0.0848$ second. The rms symmetrical component of fault current, 3,950 amperes per inch core length, is quite large being 24.7 times normal for the typical transformer of this size constant as listed in Table I. For one transformer versus two, all transformers are highly saturated. As the number of transformers is increased, the saturation in all except transformer 1 becomes less, and the harmonic component of error current decreases. For the case of one transformer versus an infinite number (which is the same as one versus a perfect transformer), the a-c and d-c components are both large, the d-c component decays slowly, and the harmonics are very small.

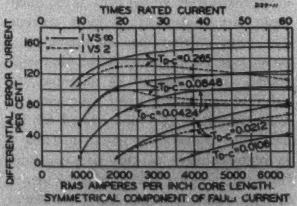

Figure 11. Maximum rms error current over one-cycle interval as a function of the magnitude of fault current and of the d-c time constant

Through-fault current, completely offset. Saturation curve 1

$C=50{,}000$ turns^2 inch/ohms

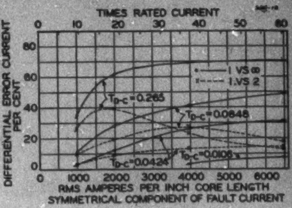

Figure 12. Rms a-c component of error current over 12-cycle interval as a function of the magnitude of fault current and of the d-c time constant

Through-fault current, completely offset. Saturation curve 1

$C=50{,}000$ turns^2 inch/ohms

Figure 7c, as compared to Figure 5d, again shows the effect of doubling the fault current for a transformer size constant $C=16{,}700$, one transformer versus five, but with a d-c time constant of 0.0848 second.

Figures 6a, 7c, and 2e show the effects of different d-c time constants in the same manner as the curves of Figure 5, and under the same conditions except for a larger fault current.

Degree of Offset

The error current of Figure 3 is for the same conditions as Figure 2d except that the d-c offset is only one half. The symmetrical component of fault current is the same in the two cases, but the maximum instantaneous fault current (and maximum possible error current) is 4,193 amperes per inch core length for Figure 3 as compared to 5,590 for Figure 2d. Although the magnitudes of the a-c and d-c components of error current are appreciably smaller, the d-c component decays at a slower rate because of less saturation. The percentage harmonic content appears to be about the same.

Saturation Characteristic

Figure 4 is also for the same conditions as Figure 2d, except that the transformers were assumed to have saturation characteristic 2, Figure 1. The maximum error currents are about the same, but the a-c component is somewhat smaller, particularly after the first two cycles. In general, the conclusions arrived at with either of these two very different saturation curves were about the same, so that they should also be valid for most other saturation curves obtainable with conventional current transformers.

Application

The character of the differential-relay current varies widely depending upon the size constant and number of transformers involved, and upon the magnitude of the fault current and its d-c time constant. For this reason it is difficult to summarize the accumulated data for all the various types of relays used, especially since the response of these relays to transient currents of this nature is not very well known.

Results can be plotted in a form which will serve to indicate the expected performance of either instantaneous or time-delay relays. For instance, Figure 8 summarizes the effect of variation of the d-c time constant for the transformer size C=16,700 for fully offset through current having a symmetrical rms component of 1,975 amperes per inch core length. The rms value of the completely offset wave is therefore equal to $\sqrt{3}\times 1{,}975$. The maximum rms differential-error current over a one-cycle interval can be taken as a measure of the current which determines the operation of an instantaneous relay. The rms a-c component of error current over the 12 cycles interval that gives the largest value is taken as a measure of the current that would determine the operation of an induction-type time-delay relay with the time setting of approximately 0.2 second. The curves were plotted for one transformer versus two and for one transformer versus an infinite number thus covering the entire expected range of operation. The data taken from Figure 5, for one transformer versus five, would lie between the two plotted curves.

Figure 9 gives more complete data for the instantaneous error currents of the same size transformer. The effects of the magnitude of fault current over a wide range are shown for three different values of d-c time constant. The fault current in rms amperes per inch core length is perfectly general. The times-rated current applies only to the particular transformer selected as representative of this size constant C=16,700. It is seen that the error current is small only in a range of very small d-c time constant and reasonably small fault current. Likewise, Figure 10 gives similar data applicable to time-delay relays. The time delay is quite effective for the smaller d-c time constants.

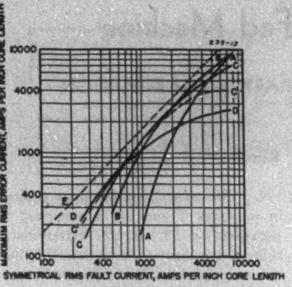

Figure 13. Differential-error current (rms over one cycle)

One transformer versus three. Through fault—fully offset. D-c time constant = 0.265 second

Curve A—Saturation characteristic 1, C=200,000 turns^2 inches/ohms

Curve B—Saturation characteristic 1, C=50,000 turns^2 inches/ohms

Curve C—Saturation characteristic 1, C=16,670 turns^2 inches/ohms

Curve C'—Saturation characteristic 2, C=16,670 turns^2 inches/ohms

Curve D—Saturation characteristic 1, C=4,170 turns^2 inches/ohms

Curve E—Current for 100 per cent error

The data of Figures 11 and 12 are similar to that of Figures 9 and 10, except that they are for the larger transformer size constant C=50,000 turns^2 inch/ohms. The times-rated fault current applies only to the particular transformer listed in Table I. The advantages of using larger transformers are apparent.

Admittedly this summary is not perfectly accurate and may not be the correct measure for determining the response of these relays. It is valuable as being indicative of definite trends.

For the high-speed relays that depend upon percentage, harmonic, or d-c restraint for proper operation, the analysis is more complicated. Their characteristics are such that each application requires special consideration and the accumulated data are valuable as a basis for selecting the proper relay characteristics.

AIR-GAP-CORE CURRENT TRANSFORMERS

The ideal solution of using current transformers large enough to insure almost perfect accuracy up to the maximum value of through-fault current is not practical because of the excessive size and cost. A new current transformer, as described by Kennedy and Sinks,[5] has been developed for high-speed bus-differential protection. This transformer, which has air gaps in the mutual flux path, has constant ratio and phase-angle errors up to the point where the iron paths saturate. By selecting transformers that maintain a linear characteristic up to the maximum value of fault current, the a-c component of primary current is always reproduced with the same degree of accuracy. Very little of the d-c component is transformed, but this is not required for differential protection. Simple overcurrent relays can be used since differential-error currents appear only because of improper matching of the linear characteristics. The size and cost is not much greater than that of the standard current transformers ordinarily used.

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The Doubly Fed Machine

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SYNCHRONOUS machines, operating with a-c excitation on both stator and rotor are used in many applications, for example, as induction frequency converters, as power and instrument Selsyn drives, and as variable speed power drives. Reference 1 has mentioned particularly the variable speed fan drive, and presented equations for the small oscillations of one such doubly fed machine. Reference 2 has also previously given the equations of hunting of the doubly fed machine (part XIV, section IV) in connection with the general study of oscillations of rotating machines. However, since the present authors have been using in their own work equations which seem to them to be more convenient and simpler in form for calculations, and since it now seems desirable to present not only general equations but also some of the more fundamental and significant performance characteristics of these machines, it is thought that this paper may now be appropriate. The form of the equations developed possesses the additional novelty of facilitating the setting up of equivalent circuits for hunting on the a-c network analyzer, and allowing the quick determination of the damping and synchronizing torques directly by wattmeter readings.

In this paper there are presented:

1. A general analysis of the doubly fed machine, in a form believed to be particularly convenient for the study of rotor hunting and for the interconnection of two or more machines.

2. An example showing the transient electrical characteristics under three-phase short circuit.

3. Examples showing the characteristic damping and synchronizing torques during hunting at various speeds and loads.

4. Equivalent electrical circuits for hunting which have been found to be of considerable help in the determination of the machine performance during small oscillations by means of the a-c network analyzer.

Results

1. The general equations are derived in appendix I and are summarized in equations 14, 23, and 24. Attention is called particularly to the compact derivation and the simplicity of the final equations made possible by the simultaneous use of complex variables in the general transient analysis and also a complex angular variation.

2. Equations 26, 27, 29, and 30, appendix II, give the transient three-phase short-circuit currents of a doubly fed machine or induction frequency converter. It is of interest here that

(*a*). The steady-state short-circuit currents are determined principally by the short-circuit or transient reactance.

(*b*). The d-c component is of the same form as that for short circuit of a synchronous machine.

(*c*). There is no transient component of fundamental frequency short-circuit current, such as would be observed in a synchronous machine; instead, the corresponding transient current is of rotor speed frequency and is moreover very small.

3. Figures 1, 2, and 3 show curves of damping and synchronizing torques for the hunting of a particular doubly fed motor. Some of the curves were calculated and some measured on the network analyzer, using the equivalent circuits of appendix IV. The damping torque is seen to be negative up to very small slips, and indeed becomes most negative at a slip somewhat greater than the frequency of hunting. It is evident that, because of the inherent negative damping characteristic of these machines, either load damping must be depended upon, or special precautions must be taken, if stable operation is to be obtained.

4. Figure 3 shows the equivalent circuits and a tabulation of the direct power and reactive kva measurements which determine the damping and synchronizing torque coefficients during hunting. The circuits are derived in a companion paper "Equivalent Circuits for the Hunting of Electrical Machinery"[3] by one of the authors.

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Appendix I. Derivation of Transient and Hunting Equations

(*a*). The equations of a two-phase rotating machine (see Figure 4, and see also reference 4, equations 10 and 11, for the relations between these two phase quantities and the usual three-phase machine quantities) referred to axes connected to the stator and rotor, are:

$$e_{as}=r_s i^{as}+p\psi_{as} \qquad e_{dr}=r_r i^{dr}+p\psi_{dr}$$
$$e_{bs}=r_s i^{bs}+p\psi_{bs} \qquad e_{qr}=r_r i^{qr}+p\psi_{qr}$$

or,

$$\boldsymbol{e}=\boldsymbol{R}\cdot\boldsymbol{i}+p\boldsymbol{\psi} \tag{1}$$

where the flux-linkage vector is $\boldsymbol{\psi}=\boldsymbol{L}\cdot\boldsymbol{i}$. In a machine with smooth air gap the self and mutual inductances are:

$$\boldsymbol{L}=\begin{array}{c|c|c|c|c} & a_s & b_s & d_r & q_r \\ \hline a_s & L_s & & M\cos\theta_2 & -M\sin\theta_2 \\ b_s & & L_s & M\sin\theta_2 & M\cos\theta_2 \\ d_r & M\cos\theta_2 & M\sin\theta_2 & L_r & \\ q_r & -M\sin\theta_2 & M\cos\theta_2 & & L_r \end{array} \tag{2}$$

where θ_2 is the angle of the machine rotor. Hence the equations are $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$, where $\boldsymbol{Z}=\boldsymbol{R}+p\boldsymbol{L}$, or

$$\boldsymbol{Z}=\begin{array}{c|c|c|c|c} & a_s & b_s & d_r & q_r \\ \hline a_s & r_s+L_sp & & pM\cos\theta_2 & -pM\sin\theta_2 \\ b_s & & r_s+L_sp & pM\sin\theta_2 & pM\cos\theta_2 \\ d_r & pM\cos\theta_2 & pM\sin\theta_2 & r_r+L_rp & \\ q_r & -pM\sin\theta_2 & pM\cos\theta_2 & & r_r+L_rp \end{array} \tag{3}$$

(*b*). Multiply the second and fourth of equations 3 by j and add them to the first and third respectively, to obtain a new set of equations

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$$e = Z \cdot i \tag{4}$$

where now

$$Z = \begin{array}{c|c|c} & s & r \\ \hline s & r_s + L_s p & M p \epsilon^{j\theta_2} \\ \hline r & M p \epsilon^{-j\theta_2} & r_r + L_r p \end{array} \tag{5}$$

and

$$\begin{aligned} e_{as} + je_{bs} &= e_s & \qquad i^{as} + ji^{bs} &= i^s \\ e_{dr} + je_{qr} &= e_r & \qquad i^{dr} + ji^{qr} &= i^r \end{aligned} \tag{6}$$

Hence the four equations with real coefficients may be expressed as two equations with complex coefficients. These equations check with reference 2, page 74, equation 23. (For the process see reference 2, page 147.)

Torque

(*a*). The electromagnetic torque upon the rotor of an electrical machine is

$$T_e = i^{dr}\psi_{qr} - i^{qr}\psi_{dr} \tag{7}$$

If $i^{dr} + ji^{qr} = i_r$, and $\psi_{dr} + j\psi_{dr} = \psi_r$, then

$$\begin{aligned} T_e &= \text{real part of } [(i^{dr} - ji^{qr})(-j)(\psi_{dr} + j\psi_{qr})] \\ &= -ji^{r*}\psi_r = i^{r*}B_r \end{aligned} \tag{8}$$

where i^{r*} is the conjugate of i^r and $B_r = -j\psi_r$ = flux-density wave of the rotor. Then from equation 5,

$$\psi_r = M\epsilon^{-j\theta}i^s + L_r i^r \tag{9}$$

Therefore, for a smooth machine:

$$T_e = \text{real part of } (-jM\epsilon^{-j\theta}i^s i^{r*}) \tag{10}$$

(*b*). The same expression for the torque may also be found from the relation:[2]

$$T = \text{real part of } i^* \cdot B = i^* \cdot G_s \cdot i$$

where the torque tensor is

$$\left.\begin{aligned} G &= \begin{array}{c|c|c} & s & r \\ \hline s & & \\ \hline r & -jM\epsilon^{-j\theta} & -jL_r \end{array} \\ G_s &= \begin{array}{c|c} & s \\ \hline r & -jM\epsilon^{-j\theta} \end{array} \end{aligned}\right\} \tag{11}$$

Transformation to Axes on Stator Flux

The stator flux rotates with a velocity $p\theta_1$ with respect to the stator (Figure 5) and the rotor flux rotates with $p\theta_3$ with respect to the rotor. The rotor itself rotates with $p\theta_2$. Hence the applied voltages are:

$$e_s = E_s\epsilon^{j\theta_1} \quad \text{and} \quad e_r = E_r\epsilon^{j\theta_3} \tag{12}$$

Let two new axes s' and r' be introduced, both rotating with the stator flux. That is, let

$$\left.\begin{aligned} i^s &= i^{s'}\epsilon^{j\theta_1} \\ i^r &= i^{r'}\epsilon^{j\theta_1}\epsilon^{-j\theta_2} = i^{r'}\epsilon^{j(\theta_1 - \theta_2)} \\ C &= \begin{array}{c|c|c} & s' & r' \\ \hline s & \epsilon^{j\theta_1} & \\ \hline r & & \epsilon^{j(\theta_1 - \theta_2)} \end{array} \end{aligned}\right\} \tag{13}$$

By $C_t^* \cdot Z \cdot C$ (where the p in Z refers to C but not to C_t^*), $C_t^* \cdot G \cdot C$, and $C_t^* \cdot e$, or by $Z' = C_t^* \cdot Z \cdot C + C_t^* \cdot L \cdot pC$

$$Z' = \begin{array}{c|c|c} & s' & r' \\ \hline s' & r_s + L_s(p + jp\theta_1) & M(p + jp\theta_1) \\ \hline r' & M[p + j(p\theta_1 - p\theta_2)] & r_r + L_r[p + j(p\theta_1 - p\theta_2)] \end{array} \qquad e' = \begin{array}{c|c} s' & E_s \\ \hline r' & E_r\epsilon^{j\delta} \end{array} \tag{14}$$

$$G_s' = \begin{array}{c|c|c} & s' & r' \\ \hline s' & & \\ \hline r' & -jM & \end{array} \qquad \delta = \theta_2 + \theta_3 - \theta_1$$

Or, writing out the equations

$$\begin{aligned} &[r_s + L_s(p + jp\theta_1)]i^{s'} + M(p + jp\theta_1)i^{r'} = E_s \\ &M[p + j(p\theta_1 - p\theta_2)]i^{s'} + \\ &\qquad \{r_r + L_r[p + j(p\theta_1 - p\theta_2)]\}i^{r'} = E_r\epsilon^{j\delta} \end{aligned} \tag{15}$$

$$\text{Torque} = \text{real of } (-jMi^{s'}i^{r'*})$$

where

$$E_s = ji^{f_1}M_1p\theta_1 = jE_1 \qquad E_r = ji^{f_3}M_3p\theta_3 = jsE_3$$

Steady State

In the steady state $p = 0$, $p\theta_1 = \omega$, $p\theta_3 = p\theta_1 - p\theta_2 = s\omega$, and $\omega L = X$. Equation 15 then becomes:

$$\left.\begin{aligned} Z' &= \begin{array}{c|c|c} & s' & r' \\ \hline s' & r_s + jX_s & jX_m \\ \hline r' & jsX_m & r_r + jsX_r \end{array} \\ e' &= \begin{array}{c|c} s' & jE_1 \\ \hline r' & jsE_3\epsilon^{j\delta} \end{array} \end{aligned}\right\} \tag{16}$$

The steady-state currents i^s and i^r required in the hunting equations of the next section, as well as the steady-state torque-angle characteristic, can be calculated from equation 16. In terms of the applied voltages, the currents are:

$$\left.\begin{aligned} i^{s'} &= \frac{jE_1\left(\dfrac{r_r}{s} + jX_r\right) + X_mE_3\epsilon^{j\delta}}{(r_s + jX_s)\left(\dfrac{r_r}{s} + jX_r\right) + X_m^2} \\ i^{r'} &= \frac{j(r_s + jX_s)E_3\epsilon^{j\delta} + X_mE_1}{(r_s + jX_s)\left(\dfrac{r_r}{s} + jX_r\right) + X_m^2} \end{aligned}\right\} \tag{17}$$

However, in many cases the stator voltage, current, and power factor, instead of both applied voltages and the angle may be assigned. Then either equation 16, or more simply the equivalent circuit shown later, can be used to calculate δ, i^r and E_3.

Hunting Equations—Small Changes in the Complex Variables

(*a*). In setting up the equations of hunting of polyphase machines, two different methods may be followed:

1. The polyphase complex equations are first changed to real equations and the latter are subjected to small changes in the variables.
2. The polyphase equations are left unchanged in complex form and are subjected to small changes in the complex variables.

The second method will be followed in this paper, as it is a more compact analytical procedure.

(*b*). Let $p\theta_2 = p\theta_{20} + \Delta p\theta_2$ and so forth; then equation 15 or 14 with the use of the compound tensor (reference 2, page 116, equation 32),

$$\begin{array}{c|c|c} & \Delta i & \Delta\theta \\ \hline \Delta e & Z & G \cdot ip - \dfrac{\partial e}{\partial\theta} \\ \hline \Delta T & -i^* \cdot (G + G_t^*) & \end{array} \tag{18}$$

give the equations of hunting as:

$$Z' = \begin{array}{c|c|c|c} & s & r & \theta \\ \hline s & r_s + L_s(p + jp\theta_1) & M(p + jp\theta_1) & \\ \hline r & M[p + j(p\theta_1 - p\theta_2)] & r_r + L_r[p + j(p\theta_1 - p\theta_2)] & -j(Mi^s + L_ri^r)p + sE_3\epsilon^{j\delta} \\ \hline \theta & jMi^{r*} & -jMi^{s*} & \end{array} \tag{19}$$

where the real part of the last row is $-\Delta T_e$.

Steady Hunting

During steady hunting let $p = kh\omega$, where h is the per unit oscillation frequency and k plays the same role as j. That is $k^2 = -1$, but kj cannot be combined. Then equations 19 become:

$$Z' = \begin{array}{c|c|c|c} & s & r & \theta \\ \hline s & r_s + X_s(kh + j) & X_m(kh + j) & \\ \hline r & X_m(kh + js) & r_r + X_r(kh + js) & -j(X_mi^s + X_ri^r)kh + sE_3\epsilon^{j\delta} \\ \hline \theta & jX_m ir^* & -jX_mi^{s*} & \end{array} \tag{20}$$

or, writing the equations out:

$$\begin{aligned} &[r_s + X_s(kh + j)]\,\Delta i^s + X_m(kh + j)\,\Delta i^r = 0 \\ &X_m(kh + js)\,\Delta i^s + [r_r + X_r(kh + js)]\,\Delta i^r = \\ &\qquad jkh(X_mi^s + X_ri^r)\,\Delta\theta_2 - sE_3\epsilon^{j\delta}\,\Delta\theta_2 - \\ &\qquad\qquad jX_mi^{r*}\,\Delta i^s + jX_mi^{s*}\,\Delta i^r = \Delta T_e \end{aligned} \tag{21}$$

where

$s = 1 - v$ = per unit rotor average slip

$\delta = \theta_2 + \theta_3 - \theta_1$

Only the real part of the last equation (ΔT) is taken.

In calculating ΔT, first the j component is discarded as usual after rationalization

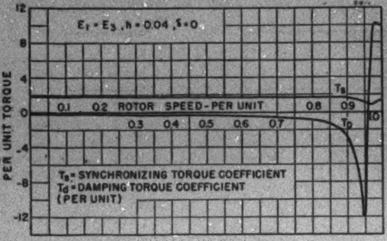


Figure 1. Damping- and synchronizing-torque coefficients of a typical doubly fed machine at no load

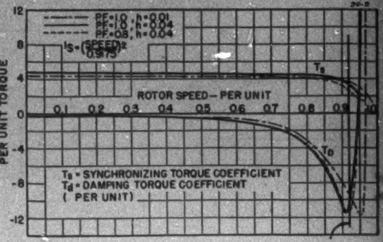


Figure 2 (right). Damping- and synchronizing-torque coefficients of a typical doubly fed machine

Effect of oscillation frequency and power factor. Load torque proportional to speed squared

so that ΔT is expressed in terms of k only (and not j). Then the $k-s$ in the denominator are rationalized, so that the synchronizing and damping torque coefficients are given by:

$$\Delta T = T_s + khT_D \qquad (22)$$

Two separate symbols j and k are used in this procedure, in order to distinguish between the complex numbers introduced by the original use of the complex j currents, voltages, and fluxes as variables, and the further complex k numbers introduced by the use of the complex (exponential) rotor angular variation. It may readily be shown that at any stage one may revert to real transient voltage equations and have only the complex numbers due to the complex angular variation.

Eliminating Δi^s

If the row and column of s is eliminated from equation 20, or the stator current change Δi^s is eliminated from 21, then

$$Z' = \begin{array}{c|c|c} & r & \theta \\ \hline r & r_r+r_s''s+khX_r''+j(sX_r''-khr_s'') & khB_r+sE_s\epsilon^{j\delta} \\ \hline \theta & -B_r^*+i^{r*}(r_s''+jX_r'') & \end{array} \qquad (23)$$

where

$$B_r = -j(X_m i^s + X_r i^r) = -j\psi_r$$

$$r_s'' = \frac{r_s X_m^2}{D'}$$

$$X_r'' = X_r - \frac{X_m^2}{D'}X_s(1-h^2) - khr_s''$$

$$D' = (r_s+khX_s)^2 + X_s^2$$

If again the rotor current Δi^r (row and column of r in equation 23) is eliminated, then the final equation for the torque change is:

$$\Delta T = \text{real of} \left[-\frac{(khB_r+sE_s\epsilon^{j\delta})[-B^*+i^{r*}(r_s''+jX_r'')]}{r_r+r_s''s+khX_r''+j(sX_r''-khr_s'')} \right] \qquad (24)$$

The phrase "real" and the asterisk (the conjugate symbol) refer to j and not to k. Both r_s'' and X_r'' are independent of the slip. First the ks are treated as algebraic symbols and the js are eliminated. Afterward the ks are treated as the complex operator k (where $k^2=-1$). After rationalizing, equation 22 holds for the torque change of the doubly fed machine. Also, if the rotor impressed voltage E_s is zero, the equations apply to the small oscillations of a standard polyphase induction motor.

Appendix II. Three-Phase Short Circuit

In order to illustrate the characteristics of the doubly fed machine under transient conditions, three-phase short circuits on both stator and rotor in turn, neglecting initial loading, will be considered.

(a). Stator Short Circuit

Applying, by superposition, a stator voltage E_s, to equations 14 or 15, we obtain for the stator three-phase short-circuit current the operational expression:

$$i^{s'} = \frac{E_s}{r_s+L_s(p+j\omega_1) - \left\{ \dfrac{M^2(p+j\omega_1)(p+j\omega_3)}{r_r+L_r(p+j\omega_3)} \right\}} \qquad (25)$$

where ω_1, ω_2, ω_3 are the constant stator frequency, rotor speed, and rotor frequency, respectively.

Taking for simplicity the stator resistance $r_s=0$, this current may be written as:

$$i^s = i^{s'}\epsilon^{j\omega_1 t} = \frac{E_s\epsilon^{j\omega_1 t}}{j\omega_1\left[L_s - \dfrac{j\omega_3 M^2}{r_r+j\omega_3 L_r}\right]} - \frac{E_s}{j\omega_1\left[L_s + \dfrac{j\omega_2 M^2}{r_r-j\omega_2 L_r}\right]} + \frac{r_r(L_r-L_r')E_s\epsilon^{-\frac{r_r}{L_r'}t+j\omega_2 t}}{L_s(j\omega_2 L_r'-r_r)(j\omega_1 L_r'+r_r)} \qquad (26)$$

where $L_r' = L_r - \dfrac{M^2}{L_s}$ = transient inductance viewed from the rotor terminals.

If the stator resistance r_s is considered, but rotor resistance $r_r=0$, the short-circuit current becomes:

$$i^s = i\epsilon^{s'j\omega_1 t} = \frac{E_s}{r_s+j\omega_1 L_s'}\left(\epsilon^{j\omega_1 t} - \epsilon^{-\frac{r_s}{L_s'}t}\right) \qquad (27)$$

where

$$L_s' = L_s - \frac{M^2}{L_r} \qquad \omega_1 L_s' = x_s'$$

In general, there are three components of current, stator fundamental frequency (ω_1), speed frequency (ω_2), and direct current. Note that with zero rotor resistance the speed frequency term disappears, leaving only the fundamental and d-c components in equation 27. The "d-c component" decays exponentially to zero, just as in case of a synchronous machine short circuit, but the usual transient component of alternating current is replaced by the speed frequency term.

(b). Rotor Short Circuit

Similarly applying a rotor voltage E_r in equations 14 or 15, the rotor short-circuit current becomes:

$$i^r = \frac{E_r}{r_r+L_r(p+j\omega_3) - \left\{ \dfrac{M^2(p+j\omega_3)(p+j\omega_1)}{r_s+L_s(p+j\omega_1)} \right\}} \qquad (28)$$

Whence, with $r_{\text{rotor}}=0$

$$i^r = i^{r'}\epsilon^{j\omega_3 t} = \frac{E_r\epsilon^{j\omega_3 t}}{j\omega_3\left[L_r - \dfrac{j\omega_1 M^2}{r_s+j\omega_1 L_s}\right]} - \frac{E_r}{j\omega_3\left[L_r - \dfrac{j\omega_2 M^2}{r_s+j\omega_2 L_s}\right]} - \frac{r_s(L_s-L_s')E_r\epsilon^{-\frac{r_s}{L_s'}t-j\omega_2 t}}{L_r(r_s+j\omega_2 L_s')(r_s+j\omega_1 L_s')} \qquad (29)$$

and with $r_{\text{stator}}=0$, but $r_{\text{rotor}}\neq 0$,

$$i^r = i^{r'}\epsilon^{j\omega_3 t} = \frac{E_r}{r_r+j\omega_3 L_r'}\left(\epsilon^{j\omega_3 t} - \epsilon^{-\frac{r_r}{L_r'}t}\right) \qquad (30)$$

The three components of current are again the fundamental (ω_3), d-c, and speed frequency (ω_2) components, but now the presence of the speed frequency term depends on the stator resistance, and the d-c decrement depends on the rotor resistance.

In case of an induction frequency converter connected between two parts of a power system, it is evident that the converter may be approximately represented simply as a reactance (the machine short-circuit reactance). Since $E_r/E_s \cong \omega_3/\omega_1$ and $L_s' \cong L_r'$ on a one-to-one turn ratio basis, the rotor and stator short-circuit currents will be nearly equal in amperes, and will also be equal in per unit if the kva base is taken proportional to the frequency of the current being considered. Thus in a network diagram involving a 25 by 60 cycle converter, all the 25-cycle system reactances may be expressed on a kva base 25 by 60

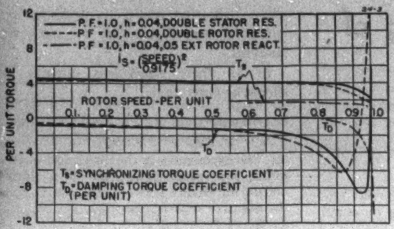


Figure 3. Damping- and synchronizing-torque coefficients of a typical doubly fed machine

Effect of change in stator and rotor resistance and in rotor reactance in machine of Figure 2

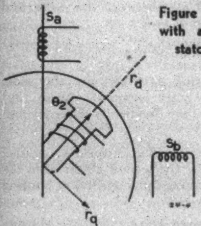


Figure 4. Machine with axes fixed in stator and rotor

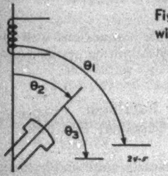


Figure 5. Machine with rotating reference axes

times as great as that for the 60-cycle system, to obtain a common diagram suitable for short circuit calculations on either part of the system.

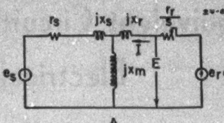
Appendix III. Damping and Synchronizing Torque—Example

Several more or less typical curves of damping and synchronizing torque coefficients as a function of rotor speed are shown in Figures 1, 2, and 3. The machine of Figure 1 has all reactances and the stator resistance about twice as great as those of Figure 2, so that with an appropriate change of scale the effect of a change in rotor resistance may be estimated. Figure 2 shows the rather slight effect of a change of stator terminal power factor, and also the effect of oscillation frequency h in shifting the point at which maximum negative damping occurs. Figure 3 shows, for the machine of Figure 2 with an oscillation frequency of $h=0.04$, the effects of doubling the stator and rotor resistances in turn, and also the effect of a greatly increased rotor leakage (or added external) reactance.

Appendix IV. Equivalent Circuits for Hunting

Figure 6 gives equivalent circuits[2] which represent the doubly fed machine during rotor hunting as well as in the steady state. In using these equivalent circuits as set up on the a-c network analyzer, the required voltages are first read from the steady-state (upper) circuit and used as indicated to compute the voltages to be applied in the hunting (lower) circuit. The total steady-state torque as well as the components of the synchronizing (T_s) and damping (T_d) torque coefficients may then be read directly on a watt and reactive voltampere meter, according to the table of formulas shown under the circuits. Note that the formulas are based on the use of a watt- and varmeter which reads e^*i to form the complex expression $W+jQ$, where W is the wattmeter reading, and Q is the varmeter reading, both here and in Figure 6. As a simple example, consider a single generator supplying a reactive load unit. If generated voltage and current flowing out of the generator are measured, the watts will be positive and the vars will be negative.

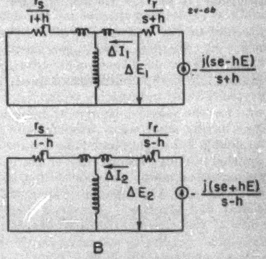
The equivalent circuits are based on the stationary axes equations given previously by one of the authors in reference 2, page 146, rather than on the rotating axes equations derived in the paper. The reason for this is that the rotating axes formulas are simpler for hand calculations, while the stationary axes equations are simpler for equivalent circuit representation. The use of rotating axes leads to d-c steady-state quantities, so that only hunting frequency terms arise during the entire calculation of either the current or torque changes, while with stationary axes the steady-state quantities are of fundamental frequency, and fundamental plus hunting and fundamental minus hunting frequency currents and voltage changes must be separately considered. On the other hand, the rotating axis equations require four separate equivalent circuits (with two meshes each) in place of three. It should be remarked that in general the choice of proper reference axes is very important if the simplest method of solution is to be had, and that the best axes depend on not only the apparatus being analyzed, but also on the aspect or particular problem being considered and on the method of solution to be used (that is, hand or network analyzer).



A—Steady-state network

$IE^* = W+jQ$

$T = W$



B—Hunting-frequency networks

$I\Delta E_1^* = W_1+jQ_1$

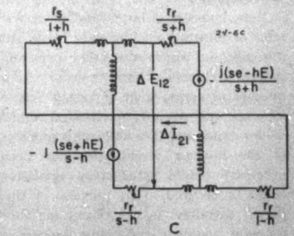
$\Delta I_1E^* = W_2+jQ_2$

$I\Delta E_2^* = W_3+jQ_3$

$\Delta I_2E^* = W_4+jQ_4$

$$T_s = \frac{W_1+W_2+W_3+W_4}{2}$$

$$T_D = \frac{(Q_2+Q_3)-(Q_1+Q_4)}{2h}$$



C—Hunting-frequency networks to simplify measurement of T_D

$I\Delta E_{12}^* = W_a+jQ_a$

$\Delta I_{12}E^* = W_b+jQ_b$

$$T_D = \frac{Q_b-Q_a}{2h}$$

Figure 6. Equivalent circuits for determination of steady-state and hunting torques

Equivalent Circuits for the Hunting of Electrical Machinery

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Synopsis: A general method is given to establish equivalent circuits for the determination of the hunting characteristics—such as damping and synchronizing torques—of standard types of electrical machines. The method is illustrated by setting up steady-state and hunting equivalent circuits for the salient-pole synchronous machine having amortisseur windings and for the doubly fed single-phase Selsyn with unbalanced windings, special cases of which are the capacitor motor and doubly fed polyphase motor. A companion paper, "The Doubly Fed Machine," contains a detailed study of the characteristics of one of the equivalent circuits as measured on the a-c network analyzer.

UNTIL recently the study of damping torque has been confined to synchronous machines and rotary converters. Operation of these machines without hunting has been obtained by the use of properly designed amortisseur or pole-face windings.

Now, however, a large number of systems of rotating machines are being put into use, in which altogether new and critical problems of hunting occur. In a number of cases, such as power Selsyn systems, and variable speed wind tunnel drives, exact and thorough going analysis of the hunting possibilities has been necessary before satisfactory operation could be secured. The purpose of this paper, therefore, is to present methods for more complete and ready analysis of these modern systems of interconnected rotating apparatus, so that hunting difficulties can be predicted and provided for in advance of installation.

The determination of the damping and synchronizing torques with standard methods involves an inordinate amount of analysis and calculation. The following equivalent circuits not only offer a clear physical picture of the interrelated phenomena taking place during small oscillations, but also enable one to get a quick numerical answer, either by the use of the a-c network analyzer, or by standard circuit methods.

The only practical equivalent circuits hitherto available were those of induction machines having symmetrical windings and running at a constant speed.[1,2] Recently steady-state equivalent circuits have been established for machines with asymmetrical windings, such as the capacitor motor,[4] also for machines with asymmetrical magnetic structure such as the salient-pole synchronous machine.[5] The extension in this paper from steady-state to hunting performance involves chiefly the application of a more complex voltage expression upon the network, and not any significant change in the network itself.

By a practical equivalent circuit is understood here one that allows the determination of not only the currents flowing and voltages appearing in every winding of the machine, but also the torques, as the speed or angle varies. The circuit must also allow the approximate consideration of the effect of saturation and iron loss.

Results

The equivalent circuits of the salient-pole alternator of Figure 1 and the necessary measurements to be made are shown in Figure 2, while those of the doubly fed single-phase Selsyn of Figure 3 are shown in Figure 4. Similar networks and measurements of other standard machines are shown on Figures 5 to 9. A detailed study of Figure 8 is undertaken in a companion paper, "The Doubly Fed Machine," showing numerous performance curves measured on the a-c network analyzer.

In all these equivalent circuits it should be noted that:

1. All equivalent circuits consist of a positive and a negative sequence network.
2. The asymmetry in the direct and quadrature axis windings appears as a single mutual impedance between the positive and the negative sequence network.
3. The asymmetry in the magnetic structure (the saliency) appears as a single mutual impedance, not only between the sequence networks, but also between the stator and rotor windings as well.

Also it should be noted that:

1. The hunting networks are the same as the steady-state networks, except that the resistances are divided by different constants.
2. Where two hunting networks are needed in place of one, the second differs from the first only in having the frequency of hunting h replaced by $-h$.
3. All impressed voltages on the hunting networks are measured off the steady-state network.
4. The steady-state torques and also the damping and synchronizing torques are measured directly by a wattmeter.

A Principle to Establish Models for Physical Systems

In setting up steady-state and hunting equivalent circuits for the various types of machines in a systematic manner, it has been found, as was to be expected, that only such collection of terms in the equations could be physically reconstructed or measured by instruments that formed a tensor. Geometric objects and other nontensor invariants could not be physically represented. Vice versa, it was also found that an equivalent circuit always gave a set of equations that formed a tensor equation.

It can be stated as an engineering principle that: A set of equations expressing

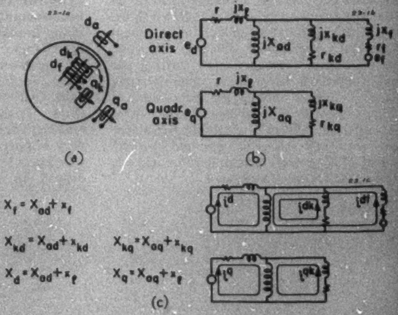

Figure 1. Reference axes and constants of the synchronous machine

(a) Reference axes

(b) Mutual and leakage reactances

(c) Assumed meshes and their self-reactances

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the performance of a physical system (be it electrical, mechanical, thermal, or any other system) may be represented by a model (equivalent circuit) only if the equations are tensor equations. That is, only tensors have physical existence, and only tensors can be measured. Vice versa, measurable physical entities always appear in the mathematical analysis as tensors.

This principle is only a consequence of previous statements that to explain the behavior of a physical system in terms of actually existing measurable physical entities, the system must be analyzed in terms of tensors. The equations of performance of simpler systems, such as stationary networks, automatically come out in a tensor form, whether they are so recognized or not, but that is not the case in more complex phenomena, such as the hunting of rotating machines. Tensors appear automatically only in special cases, while in others measurable concepts must be introduced either by experience or by the methods of tensor analysis.

Appendix I. Rules to Establish Equivalent Networks

The Determination of Torque on a Network

(*a*). When the rotor of an electrical machine rotates, at any one instant, four voltages appear in each of its windings (if all reference axes rotate with the same speed, including zero speed)

$$\begin{aligned}\mathbf{e}&=\mathbf{R}\cdot\mathbf{i}+p\boldsymbol{\phi}+p\theta\mathbf{B} \\ \mathbf{e}&=\mathbf{R}\cdot\mathbf{i}+p\mathbf{L}\cdot\mathbf{i}+p\theta\mathbf{G}\cdot\mathbf{i}\end{aligned}\quad\Bigg|\quad\begin{aligned}e_{\bar\alpha}&=R_{\bar\alpha\beta}i^\beta+p\phi_{\bar\alpha}+p\theta B_{\bar\alpha} \\ e_{\bar\alpha}&=R_{\bar\alpha\beta}i^\beta+pL_{\bar\alpha\beta}i^\beta+p\theta G_{\bar\alpha\beta}i^\beta\end{aligned}\qquad(1)$$

where

$\mathbf{e}$ = impressed voltages
$\mathbf{R}\cdot\mathbf{i}$ = resistance drops
$p\boldsymbol{\phi}$ = induced voltages
$p\theta\mathbf{B}$ = voltage generated by cutting the rotor flux-density wave $\mathbf{B}$

The instantaneous torque on the rotor is the real part of

$$T=\mathbf{i}^*\cdot\mathbf{B}=\mathbf{i}^*\cdot\mathbf{G}\cdot\mathbf{i}\ \Big|\ T=i^\alpha B_{\bar\alpha}=i^\alpha G_{\bar\alpha\beta}i^\beta\qquad(2)$$

(*b*). The voltage equation 1 may be written

$$\mathbf{e}=\mathbf{Z}\cdot\mathbf{i}\ \Big|\ e_{\bar\alpha}=Z_{\bar\alpha\beta}i^\beta\qquad(3)$$

where the transient impedance tensor is defined as

$$\mathbf{Z}=\mathbf{R}+\mathbf{L}p+p\theta\mathbf{G}\quad\Big|\quad Z_{\bar\alpha\beta}=R_{\bar\alpha\beta}+L_{\bar\alpha\beta}p+p\theta G_{\bar\alpha\beta}\qquad(4)$$

In rotating machinery $\mathbf{G}$, hence $\mathbf{Z}$ is asymmetrical.

If the impedance tensor $\mathbf{Z}$ is symmetrical it is often possible to establish a stationary network whose performance is also $\mathbf{e}=\mathbf{Z}\cdot\mathbf{i}$. In all standard electrical machines it is possible to introduce a transformation $\mathbf{C}$ that changes the asymmetrical $\mathbf{Z}$ to a symmetrical $\mathbf{Z}'$, thereby allowing the establishment of an equivalent network.

The selection of the form of $\mathbf{C}$ depends on the desired reference frame to be employed and the desired form of the equivalent circuit. Hence $\mathbf{C}$ is different for different types of machines and interconnected systems.

(*c*). Since the equivalent network—when found with the aid of $\mathbf{C}$—corresponds to the equation of voltage 1 all the individual voltages $\mathbf{R}\cdot\mathbf{i}$ or $p\theta\mathbf{B}$ can be found on the network. Hence on all equivalent networks the rotor flux-density vector $\mathbf{B}$ appears as a set of differences of potential $\mathbf{E}$ (to a suitable scale) and can be easily traced on the network by reading the components of $\mathbf{G}\cdot\mathbf{i}$.

The torque then is given (in synchronous watts) by the wattmeter readings

$$\text{Torque}=T=\text{real part of }\mathbf{i}^*\mathbf{B}=\mathbf{i}^*\cdot\mathbf{E}\qquad(5)$$

where

$$\mathbf{E}=\mathbf{G}\cdot\mathbf{i}\qquad(6)$$

It has to be remembered that:

1. If in a product i and E are of different frequencies, then iE^* is divided by two, giving the peak of the oscillating torque.
2. If both i and E are of the same frequency (including zero frequency) or if only one of them is of zero frequency, then iE^* is not divided by two.

The Equations of Small Displacements

(*a*). When the rotor oscillates for any cause, θ becomes $\theta_0+\Delta\theta$, $\mathbf{i}$ becomes $\mathbf{i}_0+\Delta\mathbf{i}$, and so on, so that the above two equations become

$$\mathbf{e}+\Delta\mathbf{e}=\mathbf{R}(\mathbf{i}+\Delta\mathbf{i})+p(\boldsymbol{\phi}+\Delta\boldsymbol{\phi})+p(\theta+\Delta\theta)(\mathbf{B}+\Delta\mathbf{B})\qquad(7)$$

$$T+\Delta T=\text{real part of }-(\mathbf{i}+\Delta\mathbf{i})^*\cdot(\mathbf{B}+\Delta\mathbf{B})+Mp^2(\theta+\Delta\theta)\qquad(8)$$

(where now T represents applied torque, hence the minus sign).

Subtracting the steady-state equations and neglecting second-order infinitesimals, the equations of small displacements are

$$\Delta\mathbf{e}=\mathbf{R}\cdot\Delta\mathbf{i}+p\Delta\boldsymbol{\phi}+p\theta\Delta\mathbf{B}+\Delta p\theta\mathbf{B}\qquad(9)$$

$$\Delta T=-(\mathbf{i}^*\cdot\Delta\mathbf{B}+\Delta\mathbf{i}^*\cdot\mathbf{B})+Mp^2\Delta\theta\qquad(10)$$

(The phrase "Real part of" will be left out in the following.)

Or in terms of constants

$$\begin{aligned}\Delta\mathbf{e}&=\mathbf{R}\cdot\Delta\mathbf{i}+\mathbf{L}\cdot p\Delta\mathbf{i}+p\theta\mathbf{G}\cdot\Delta\mathbf{i}+\Delta p\theta\mathbf{G}\cdot\mathbf{i} \\ \Delta T&=-(\mathbf{i}^*\cdot\mathbf{G}\cdot\Delta\mathbf{i}+\Delta\mathbf{i}^*\cdot\mathbf{G}\cdot\mathbf{i})+Mp^2\Delta\theta\end{aligned}\qquad(11)$$

(*b*). The equation of small displacement may be written as

$$\Delta\mathbf{e}=\mathbf{Z}\cdot\Delta\mathbf{i}+\Delta p\theta\mathbf{G}\cdot\mathbf{i}$$

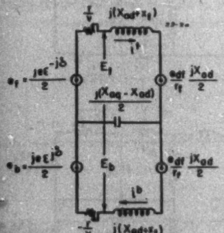
or

$$\boxed{\mathbf{Z}\cdot\Delta\mathbf{i}=\left(\frac{\partial\mathbf{e}}{\partial\theta}-\mathbf{B}p\right)\Delta\theta}\qquad(12)$$

where $\mathbf{Z}$ is defined in equation 4.

Hence during small oscillations the transient impedance tensor $\mathbf{Z}$ of any machine is the same as the $\mathbf{Z}$ during constant speed. However, two sets of impressed voltages appear:

1. $-\mathbf{B}\Delta p\theta$ representing the internal generated voltages due to cutting the steady-state rotor flux-



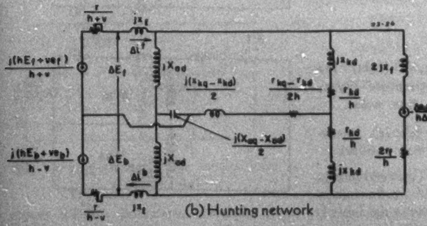
$$i^fE_f^*=W'\qquad T=W'+W''$$
$$i^bE_b^*=W''$$

$$i^f\Delta E_f^*=W_1+jQ_1\qquad\Delta i^fE_f^*=W_3+jQ_3$$
$$i^b\Delta E_b^*=W_2+jQ_2\qquad\Delta i^bE_b^*=W_4+jQ_4$$

$$T_s=W_1+W_2+W_3+W_4$$

$$T_D=\frac{(Q_3+Q_4)-(Q_1+Q_2)}{h}$$

(a) (left) Steady-state network (c) Torque formulas



(b) Hunting network

Figure 2. The salient-pole synchronous machine on infinite bus

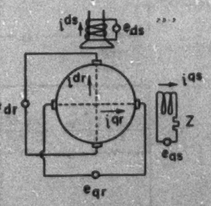


Figure 3. Reference axes of the doubly fed single-phase motor

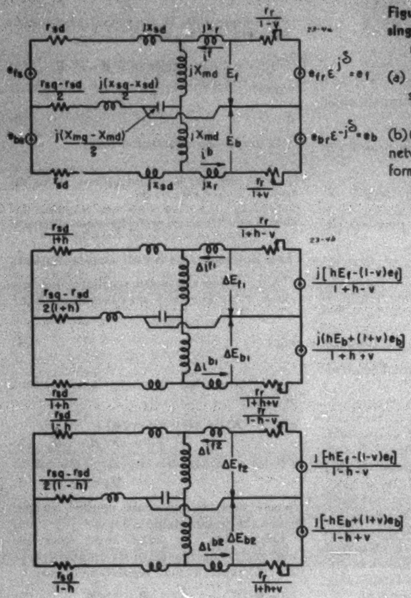

23-V4a
r_{sd}
jx_{sd}
jx_r
$\frac{r_r}{1-v}$
e_{fs}
$\frac{r_{sq}-r_{sd}}{2}$
$\frac{j(x_{sq}-x_{sd})}{2}$
jX_{md}
E_f
i^f
$e_{fr}\varepsilon^{j\delta}=e_f$
e_{bs}
$\frac{j(X_{mq}-X_{md})}{2}$
jX_{md}
E_b
i^b
$e_{br}\varepsilon^{-j\delta}=e_b$
r_{sd}
jx_{sd}
jx_r
$\frac{r_r}{1+v}$
23-V4b
$\frac{r_{sd}}{1+h}$
$\frac{r_r}{1+h-v}$
Δi^{f1}
ΔE_{f1}
$\frac{r_{sq}-r_{sd}}{2(1+h)}$
$\frac{j[hE_f-(1-v)e_f]}{1+h-v}$
$\frac{j(hE_b+(1+v)e_b]}{1+h+v}$
ΔE_{b1}
Δi^{b1}
$\frac{r_{sd}}{1+h}$
$\frac{r_r}{1+h+v}$
$\frac{r_{sd}}{1-h}$
$\frac{r_r}{1-h-v}$
Δi^{f2}
ΔE_{f2}
$\frac{r_{sq}-r_{sd}}{2(1-h)}$
$\frac{j[-hE_f-(1-v)e_f]}{1-h-v}$
$\frac{j[-hE_b+(1+v)e_b]}{1-h+v}$
Δi^{b2}
ΔE_{b2}
$\frac{r_{sd}}{1-h}$
$\frac{r_r}{1+h+v}$

Figure 4 (left). The single-phase instrument Selsyn

(a) (above) Steady-state network

(b) (below) Hunting networks (for torque formulas see Table I)

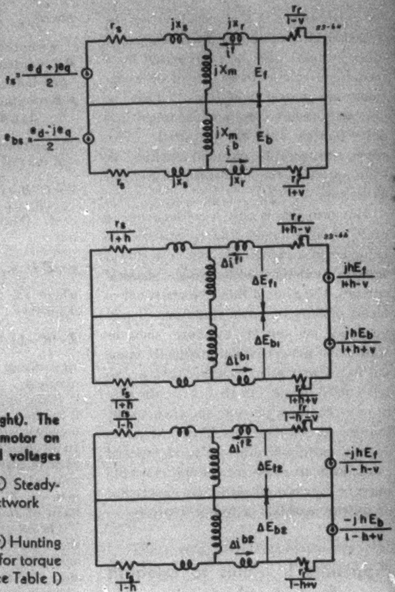

23-6a
r_s
jx_s
jx_r
$\frac{r_r}{1-v}$
$e_{fs}=\frac{e_d+je_q}{2}$
jX_m
E_f
i^f
$e_{bs}=\frac{e_d-je_q}{2}$
jX_m
E_b
i^b
r_s
jx_s
jx_r
$\frac{r_r}{1+v}$
$\frac{r_s}{1+h}$
$\frac{r_r}{1+h-v}$
23-6b
Δi^{f1}
ΔE_{f1}
$\frac{jhE_f}{1+h-v}$
ΔE_{b1}
$\frac{jhE_b}{1+h+v}$
Δi^{b1}
$\frac{r_s}{1+h}$
$\frac{r_r}{1+h+v}$
$\frac{r_s}{1-h}$
$\frac{r_r}{1-h-v}$
Δi^{f2}
ΔE_{f2}
$\frac{-jhE_f}{1-h-v}$
ΔE_{b2}
$\frac{-jhE_b}{1-h+v}$
Δi^{b2}
$\frac{r_s}{1-h}$
$\frac{r_r}{1-h+v}$

Figure 6 (right). The induction motor on unbalanced voltages

(a) (above) Steady-state network

(b) (below) Hunting networks (for torque formulas see Table I)

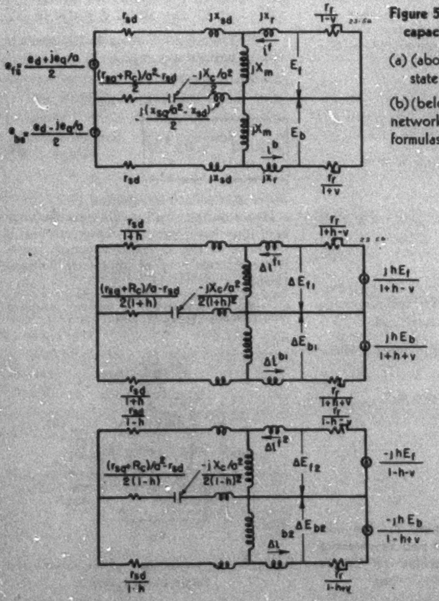

23-5a
r_{sd}
jx_{sd}
jx_r
$\frac{r_r}{1-v}$
$e_{fs}=\frac{e_d+je_q/a}{2}$
$\frac{(r_{sq}+R_c)/a^2-r_{sd}}{2}$
$\frac{-jX_c/a^2}{2}$
$\frac{j(x_{sq}/a^2-x_{sd})}{2}$
i^f
jX_m
E_f
$e_{bs}=\frac{e_d-je_q/a}{2}$
jX_m
E_b
i^b
r_{sd}
jx_{sd}
jx_r
$\frac{r_r}{1+v}$
$\frac{r_{sd}}{1+h}$
$\frac{r_r}{1+h-v}$
23-5b
Δi^{f1}
ΔE_{f1}
$\frac{(r_{sq}+R_c)/a-r_{sd}}{2(1+h)}$
$\frac{-jX_c/a^2}{2(1+h)^2}$
$\frac{jhE_f}{1+h-v}$
ΔE_{b1}
$\frac{jhE_b}{1+h+v}$
Δi^{b1}
$\frac{r_{sd}}{1+h}$
$\frac{r_r}{1+h+v}$
$\frac{r_{sd}}{1-h}$
$\frac{r_r}{1-h-v}$
Δi^{f2}
ΔE_{f2}
$\frac{(r_{sq}+R_c)/a^2-r_{sd}}{2(1-h)}$
$\frac{-jX_c/a^2}{2(1-h)^2}$
$\frac{-jhE_f}{1-h-v}$
ΔE_{b2}
Δi^{b2}
$\frac{-jhE_b}{1-h+v}$
$\frac{r_{sd}}{1-h}$
$\frac{r_r}{1-h+v}$

Figure 5 (left). The capacitor motor

(a) (above) Steady-state network

(b) (below) Hunting networks (for torque formulas see Table I)

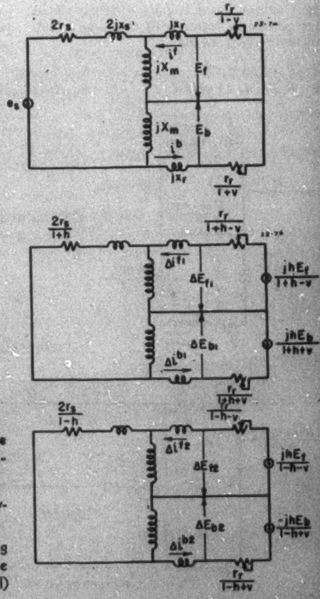

23-7a
$2r_s$
$2jx_s'$
jx_r
$\frac{r_r}{1-v}$
i^f
jX_m
E_f
e_s
jX_m
E_b
i^b
jx_r
$\frac{r_r}{1+v}$
$\frac{2r_s}{1+h}$
$\frac{r_r}{1+h-v}$
23-7b
Δi^{f1}
ΔE_{f1}
$\frac{jhE_f}{1+h-v}$
ΔE_{b1}
$\frac{jhE_b}{1+h+v}$
Δi^{b1}
$\frac{r_r}{1+h+v}$
$\frac{2r_s}{1-h}$
$\frac{r_r}{1-h-v}$
Δi^{f2}
ΔE_{f2}
$\frac{jhE_f}{1-h-v}$
ΔE_{b2}
$\frac{-jhE_b}{1-h+v}$
Δi^{b2}
$\frac{r_r}{1-h+v}$

Figure 7 (right). The single-phase induction motor

(a) (above) Steady-state network

(b) (below) Hunting networks (for torque formulas see Table I)

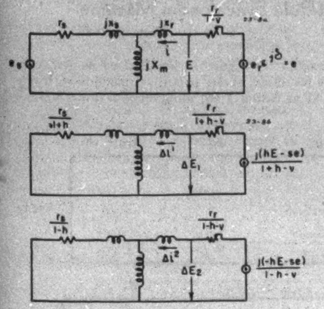


Figure 8. The doubly fed polyphase induction motor

(a) (left) Steady-state network
(b) (left) Hunting networks
(c) (right) Torque formulas

$$iE^* = W \qquad T = W$$
$$i\Delta E_1^* = W_1 + jQ_1 \qquad \Delta i^1 E^* = W_3 + jQ_3$$
$$i\Delta E_2^* = W_2 + jQ_2 \qquad \Delta i^2 E^* = W_4 + jQ_4$$
$$T_s = \frac{W_1 + W_2 + W_3 + W_4}{2} \qquad T_D = \frac{(Q_2+Q_3)-(Q_1+Q_4)}{2h}$$

density $\mathbf{B}$ by the oscillating speed change $\Delta p\theta$. This voltage appears in every machine.

2. $\partial e/\partial\theta$ representing the oscillation of the steady-state impressed voltage e. This voltage appears only in slip ring machines where e is a function of θ.

Also small voltage changes Δf may be impressed from outside.

Steady Hunting

(*a*). When the frequency of oscillations—say in spontaneous hunting or in driving a reciprocating load—is $h\omega$, and p is to be replaced by $jh\omega'$, care has to be exercised if the transient $\mathbf{Z}$ contains the complex operator j. Such cases can be analytically treated by introducing an additional complex operator k for $p = kh\omega'$ as shown in the companion paper "The Doubly Fed Machine." However to establish equivalent networks it is advisable to use a physical reference frame that does not introduce j in the transient $\mathbf{Z}$, so as to avoid using equivalent networks with two sets of frequencies.

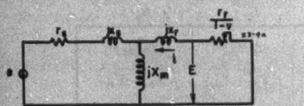
(*b*). In replacing p by $jh\omega'$ (assuming no j in the transient $\mathbf{Z}$) two cases have to be distinguished (reference 7, page 119) depending on the reference frame used:

1. The steady-state currents i are constant. Then all p in equation 12 are replaced by $jh\omega$.

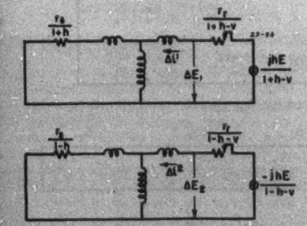
2. The steady-state currents i are of fundamental frequency. In such a case two sets of hunting equations are established:

(*a*). The currents Δi are of fundamental plus hunting frequency. Then all p in $\mathbf{Z}$ are replaced by $j(1+h)\omega$ and p in $\mathbf{B}p\Delta\theta$ by $jh\omega$.

(*b*). The currents Δi are of fundamental minus hunting frequency. Then all p in $\mathbf{Z}$ become $j(1-h)\omega$ and p in $\mathbf{B}p\Delta\theta$ becomes $-jh\omega$. That is h assumes a negative value.



(a) Steady-state network



(b) Hunting networks (for torque formulas see Figure 8c)

Figure 9. The polyphase induction motor

(*c*). The hunting-frequency electrical torque ΔT comes out as a complex number

$$\Delta T = \frac{\partial T}{\partial\theta}\Delta\theta = (T_s + jhT_D)\Delta\theta \tag{13}$$

where T_s is the synchronizing-torque coefficient and T_D the damping-torque coefficient. If one of them is negative, the system is unstable. In calculating T_s and T_D only $\Delta T/\Delta\theta$ is needed, hence in the impressed voltage $\Delta\theta$ may be left out or assumed to be any convenient constant.

$$\mathbf{C} = \frac{1}{2}\begin{array}{c|cc} & f & b \\ \hline d & 1 & 1 \\ q & -j & j \end{array} \qquad \gamma = \begin{array}{c|cc} & d & q \\ \hline d & & -1 \\ q & 1 & \end{array}$$

If $\Delta\theta$ is given, the oscillating-frequency torque is

$$\Delta T = \Delta\theta\sqrt{T_s^2 + (hT_D)^2} \tag{14}$$

(*d*). Once p is replaced by $jh\omega$, and so on, it is permissible to introduce a $\mathbf{C}$ containing j.

Steps to Reduce $\mathbf{Z}$ to a Symmetrical Form

(*a*). In machines with sinusoidal space waves the torque tensor $\mathbf{G}$ may be expressed along any reference frame as

$$\mathbf{G} = \gamma\cdot\mathbf{L} \quad \Big| \quad G_{\alpha\beta} = \gamma_\alpha^{\ \gamma} L_{\gamma\beta} \tag{15}$$

where γ is the rotation tensor. (Reference 7, page 62.) Hence the impedance tensor may be written as

$$\mathbf{Z} = \mathbf{R} + (p\mathbf{I} + p\theta\gamma)\cdot\mathbf{L} \tag{16}$$

where $\mathbf{I}$ is the unit tensor. The tensors $\mathbf{R}$, $\mathbf{I}$, and $\mathbf{L}$ are symmetrical in any reference frame, but γ is not. It is assumed that $\mathbf{Z}$ contains no j.

(*b*). To establish an equivalent network it is necessary to bring $\mathbf{Z}$ to a symmetrical form. Three steps are to be made:

1. Replacing p by $j\omega$ and $p\theta$ by $v\omega$,

$$\mathbf{Z} = \mathbf{R} + (\mathbf{I} - jv\gamma)\cdot j\mathbf{X} = \mathbf{R} + \mathbf{n}\cdot j\mathbf{X} \tag{17}$$

where $\mathbf{n}$ contains the speed v as a parameter

$$\mathbf{n} = \mathbf{I} - jv\gamma \tag{18}$$

2. Bringing $\mathbf{Z}$ to a symmetrical form consists chiefly of discovering a transformation $\mathbf{C}$ that changes the rotation tensor γ to a diagonal form. When the physical reference axes have been judiciously selected and also are at right angles in space, one such $\mathbf{C}$ is that of the method of symmetrical components

$$\gamma' = \begin{array}{c|cc} & f & b \\ \hline f & j & \\ b & & -j \end{array} \tag{19}$$

where $\gamma' = \mathbf{C}_t^*\cdot\gamma\cdot\mathbf{C}$. This step brings γ, hence $\mathbf{n}$ to a diagonal form. Except for the scalars n, the matrix of $\mathbf{Z}$ is now symmetrical.

Since the sequence axes are hypothetical axes, their introduction does not change the frequency of any quantity, and it is immaterial whether first the ps are replaced by $jh\omega$ and then are the sequence axes introduced or first the sequence axes are introduced, and then the ps are replaced by $jh\omega$.

3. Dividing the equation of voltage $e = \mathbf{Z}\cdot i$ by n (or each equation by an appropriate scalar)

$$e' = \mathbf{n}^{-1}\cdot e = (\mathbf{n}^{-1}\cdot\mathbf{R} + j\mathbf{X})\cdot i = \mathbf{Z}'\cdot i \tag{20}$$

where $\mathbf{Z}'$ now is symmetrical. The resistances and the impressed voltages are functions of the speed v.

To find the torque, not only $\mathbf{Z}$ but also $\mathbf{G}$ must be transformed by $\mathbf{C}$. Then $\omega\mathbf{G}\cdot i = \mathbf{E}$ is traced on the network. The torque is the real part of $i\cdot\mathbf{E}^*$, that is the sum of the wattmeter readings.

Steps to Establish the Steady-State Networks

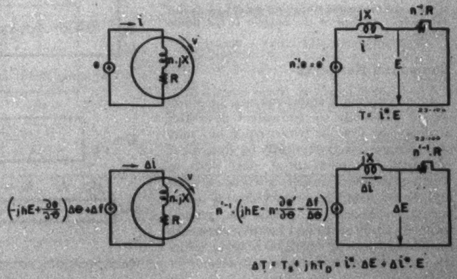
1. The transient $\mathbf{Z}$ of the machine is first set up along a reference frame that gives a symmetrical $\mathbf{Z}$.

2. The torque tensor $\mathbf{G}$ and impressed voltage vector e are also found along the same reference frame.

Figure 10. Compound machines and their compound networks

(a) Steady-state machine and its equivalent network

(b) Hunting machine and its equivalent network


i
v
e
$n\cdot jX$
R
jX
$n^{-1}\cdot R$
$n^{-1}\cdot e = e'$
E
$T = i^*\cdot E$
Δi
$n'\cdot jX$
$\left(-jhE + \frac{\partial e}{\partial\theta}\right)\Delta\theta + \Delta f$
$n'^{-1}\cdot R$
$n'^{-1}\cdot\left(jhE - n\cdot\frac{\partial e'}{\partial\theta} - \frac{\Delta f}{\Delta\theta}\right)$
ΔE
$\Delta T = T_s + jhT_D = i^*\cdot\Delta E + \Delta i^*\cdot E$

3. Replace p in $\boldsymbol{Z}$ by $j\omega$ (where ω may be zero).

4. Divide $\boldsymbol{Z}$ by $\boldsymbol{n}$.

5. Establish the steady-state network.

6. The impressed voltages are $\boldsymbol{e}'=\boldsymbol{n}^{-1}\cdot\boldsymbol{e}$.

7. The differences of potential $\boldsymbol{E}=\omega\boldsymbol{G}\cdot\boldsymbol{i}$ are indicated on the equivalent circuit.

8. The steady-state torque is the real part of $\boldsymbol{i}^*\cdot\boldsymbol{E}$.

Steps to Establish the Hunting Networks

1. Replace p in $\boldsymbol{Z}$ by $j\omega'$ (where ω' is $h\omega$ or $(1\pm h)\omega$ as indicated in a previous section).

2. Divide $\boldsymbol{Z}$ by $\boldsymbol{n}'$.

3. Establish the corresponding network or networks. They are the same as the steady-state network except that the resistances are divided by different constants $\boldsymbol{n}'$.

4. The voltages impressed on the hunting networks are

$$\Delta\boldsymbol{e}'=\boldsymbol{n}'^{-1}\cdot\left(jh\boldsymbol{E}-\boldsymbol{n}\cdot\frac{\partial\boldsymbol{e}'}{\partial\theta}-\frac{\Delta\boldsymbol{f}}{\Delta\theta}\right) \qquad (21)$$

where $\boldsymbol{E}$ and $\boldsymbol{e}'$ appear on the steady-state network, $\Delta\boldsymbol{f}$ is any outside impressed voltage change and h may be plus or minus. Also $\boldsymbol{n}\cdot\partial\boldsymbol{e}'/\partial\theta=\partial\boldsymbol{e}/\partial\theta$.

5. On the hunting network the differences of potential $\Delta\boldsymbol{E}=\omega\boldsymbol{G}\cdot\Delta\boldsymbol{i}$ are determined (they exist across the same junction-pairs as $\boldsymbol{E}$).

6. The following watts W and vars Q are measured

$$\Delta T/\Delta\theta=W+jQ=\boldsymbol{i}\cdot\Delta\boldsymbol{E}^*+\Delta\boldsymbol{i}\cdot\boldsymbol{E}^*=T_s+jhT_D \qquad (22)$$

7. The sum of the wattmeter readings is the synchronizing torque T_s and the sum of the varmeter readings (divided by h) is the damping torque T_D.

It should be remembered that:

1. By convention the varmeter reading $W+jQ$ is $\boldsymbol{i}\cdot\boldsymbol{E}^*$ and not $\boldsymbol{i}^*\cdot\boldsymbol{E}$; hence a negative Q gives positive T_D.

2. While i and E in the actual machine are rotating vectors $i=(A+jB)\epsilon^{j\omega t}$; ΔT, also i and E in the networks are single-phase quantities, $i=$real of $(C+jD)\epsilon^{j\omega t}$. When the frequency of a ΔT expression is $-\omega_h$ it represents a negative T_D.

Hence if the frequency of ΔT is $-\omega_h$ the varmeter reading Q keeps its sign. If ΔT is $+\omega_h$, the sign of Q is reversed.

3. If a ΔT expression is the product of two sinusoidal waves, one half of the product is the torque change.

Compound Networks

Just as ordinary equations may be represented physically by equivalent networks, analogously a set of tensor equations may also be represented physically by equivalent networks (see reference 6, page 480) in which each coil represents a whole network and each current represents several mesh-currents. Figure 10 shows the general form of steady-state and hunting networks for all machines in which no relative velocity exists between the reference axes.

Appendix II. The Salient-Pole Synchronous Machine

The Primitive Machine

Let the constants of the synchronous machine of Figure 1a be assumed as shown in Figures 1b and c. Its $\boldsymbol{Z}$, $\boldsymbol{G}$, and $\boldsymbol{e}$ tensors (or those of the primitive machine with five axes) have been given in reference 7, page 42 as (using X for inductance L in per unit as is customary in the per-unit system)

$$\boldsymbol{Z}=\begin{array}{c|c|c|c|c|c} & d_f & d_k & q_k & d_a & q_a \\ \hline d_f & r_f+X_fp & X_{ad}p & & X_{ad}p & \\ d_k & X_{ad}p & r_{kd}+X_{kd}p & & X_{ad}p & \\ q_k & & & r_{kq}+X_{kq}p & & X_{aq}p \\ d_a & X_{ad}p & X_{ad}p & -X_{aq}p\theta & r+X_dp & -X_qp\theta \\ q_a & X_{ad}p\theta & X_{ad}p\theta & X_{aq}p & X_dp\theta & r+X_qp \end{array} \qquad (23)$$

$$\boldsymbol{G}=\begin{array}{c|c|c|c|c|c} & d_f & d_k & q_k & d_a & q_a \\ \hline d_a & & & -X_{aq} & & -X_q \\ q_a & X_{ad} & X_{ad} & & X_d & \end{array} \qquad (24)$$

$$\boldsymbol{e}=\begin{array}{c|c|c|c|c} d_f & d_k & q_k & d_a & q_a \\ \hline e_{df} & & & p\theta e\sin\delta & p\theta e\cos\delta \end{array} \qquad (25)$$

where $\delta=\theta-\theta_{\text{bus}}$. All the five reference axes are rigidly attached to the field and rotate with it. (Note the change in sign of $p\theta$.)

The machine is assumed to be connected to an infinite bus running at the same speed $p\theta$ as the field.

Reduction to Symmetrical Form

Let symmetrical components be introduced by the following transformation

$$\begin{aligned} i^{df}&=i^{df}\\ i^{dk}&=(i^{fk}+i^{bk})/2\\ i^{qk}&=-j(i^{fk}-i^{bk})/2\\ i^{da}&=(i^{fa}+i^{ba})/2\\ i^{qa}&=-j(i^{fa}-i^{ba})/2 \end{aligned} \qquad (26)$$

$$\boldsymbol{C}=\frac{1}{2}\begin{array}{c|c|c|c|c|c} & d_f & f_k & b_k & f_a & b_a \\ \hline d_f & 2 & & & & \\ d_k & & 1 & 1 & & \\ q_k & & -j & j & & \\ d_a & & & & 1 & 1 \\ q_a & & & & -j & j \end{array} \qquad (27)$$

If now:

1. $\boldsymbol{C}_t^*\cdot\boldsymbol{Z}\cdot\boldsymbol{C}=\boldsymbol{Z}'$, $\boldsymbol{C}_t^*\cdot\boldsymbol{G}\cdot\boldsymbol{C}=\boldsymbol{G}'$ and $\boldsymbol{C}_t^*\cdot\boldsymbol{e}=\boldsymbol{e}'$ are calculated,

2. p is replaced by jh and $p\theta$ by v where v is the per-unit velocity of the machine,

3. $\boldsymbol{Z}'$ is multiplied by $\boldsymbol{n}'^{-1}$ where the value of $\boldsymbol{n}'$ is given in equation 28.

$$\boldsymbol{n}'=\begin{array}{c|c|c|c|c|c} & d_f & d_k & q_k & d_a & q_a \\ \hline d_f & h & & & & \\ f_k & & h & & & \\ b_k & & & h & & \\ f_a & & & & h+v & \\ b_a & & & & & h-v \end{array} \qquad (28)$$

The results are

$$\boldsymbol{n}'^{-1}\cdot\boldsymbol{Z}'=1/4\times\begin{array}{c|c|c|c|c|c} & d_f & f_k & b_k & f_a & b_a \\ \hline d_f & 4\left(\frac{r_f}{h}+jX_f\right) & 2jX_{ad} & 2jX_{ad} & 2jX_{ad} & 2jX_{ad} \\ f_k & 2jX_{ad} & (r_{kd}+r_{kq})/h+j(X_{kd}+X_{kq}) & (r_{kd}-r_{kq})/h+j(X_{ad}-X_{aq}) & j(X_{ad}+X_{aq}) & j(X_{ad}-X_{aq}) \\ b_k & 2jX_{ad} & (r_{kd}-r_{kq})/h+j(X_{ad}-X_{aq}) & (r_{kd}+r_{kq})/h+j(X_{kd}+X_{kq}) & j(X_{ad}-X_{aq}) & j(X_{ad}+X_{aq}) \\ f_a & 2jX_{ad} & j(X_{ad}+X_{aq}) & j(X_{ad}-X_{aq}) & 2r/(h+v)+j(X_d+X_q) & j(X_d-X_q) \\ b_a & 2jX_{ad} & j(X_{ad}-X_{aq}) & j(X_{ad}+X_{aq}) & j(X_d-X_q) & 2r/(h-v)+j(X_d+X_q) \end{array} \qquad (29)$$

The same result is found if first p is replaced by $jh\omega$, then $\boldsymbol{C}$ is introduced.

$$\boldsymbol{G}=\frac{1}{4}\begin{array}{c|c|c|c|c|c} & d_f & f_k & b_k & f_a & b_a \\ \hline f_a & 2jX_{ad} & j(X_{ad}+X_{aq}) & j(X_{ad}-X_{aq}) & j(X_d+X_q) & j(X_d-X_q) \\ b_a & -2jX_{ad} & -j(X_{ad}-X_{aq}) & -j(X_{ad}+X_{aq}) & -j(X_d-X_q) & -j(X_d+X_q) \end{array} \qquad (30)$$

$$\boldsymbol{e}=\begin{array}{c|c|c|c|c} d_f & f_k & b_k & f_a & b_a \\ \hline e_{df} & & & jve\epsilon^{-j\delta}/2 & -jve\epsilon^{j\delta}/2 \end{array} \qquad (31)$$

$$=\begin{array}{c|c|c|c|c} d_f & f_k & b_k & f_a & b_a \\ \hline e_{df} & & & ve_f & -ve_b \end{array} \qquad (32)$$

where e_f and e_b appear on the steady-state network of Figure 2a derived in reference 5.

The Hunting Equivalent Network

(*a*). The equivalent circuit of hunting (with per-phase constants) corresponding to Z' is shown in Figure 2b. It is established in the same manner as the steady-state network shown in reference 5.

It should be noted that at least two negative resistances are needed, one for each network. Such negative resistances may be used in conjunction with the a-c network analyzer. If no negative resistances are at hand, the networks of Figure 2 may be changed in a manner similar to that shown in reference 5, Figures 3b, 6, and 7.

(*b*). The components of $\Delta E=G'\cdot\Delta i'$ are shown in Figure 2b.

$$\Delta E=\frac{1}{4}\begin{array}{c|r|} \hline f_a & 2jX_{ad}\Delta i^{df}+j(X_{ad}+X_{aq})\Delta i^{fk}+j(X_{ad}-X_{aq})\Delta i^{bk}+ \\ & j(X_d+X_q)\Delta i^{fa}+j(X_d-X_q)\Delta i^{ba} \\ \hline b_a & -[2jX_{ad}\Delta i^{df}+j(X_{ad}-X_{aq})\Delta i^{fk}+j(X_{ad}+X_{aq})\Delta i^{bk}+ \\ & j(X_d-X_q)\Delta i^{fa}+j(X_d+X_q)\Delta i^{ba}] \\ \hline \end{array} \qquad (33)$$

$$=\begin{array}{|c|c|c|c|c|} d_f & f_k & b_k & f_a & b_a \\ \hline & & & \Delta E_f & \Delta E_b \\ \hline \end{array} \qquad (34)$$

(*c*). To find the impressed voltages Δe on the hunting network, let equation 31 be differentiated

$$\frac{\partial e}{\partial\theta}=\begin{array}{|c|c|c|c|c|} d_f & f_k & b_k & f_a & b_a \\ \hline & & & -jve_f & -jve_b \\ \hline \end{array} \qquad (35)$$

Hence the resultant Δe is

$$\Delta e=n'^{-1}\left(jhE-\frac{\partial e}{\partial\theta}-\frac{\Delta f}{\Delta\theta}\right) \qquad (36)$$

$$\Delta e=\begin{array}{|c|c|c|c|c|} d_f & f_k & b_k & f_a & b_a \\ \hline -\frac{\Delta e_{df}}{h\,\Delta\theta} & & & \frac{j(hE_f+ve_f)}{h+v} & \frac{j(hE_b+ve_b)}{h-v} \\ \hline \end{array} \qquad (37)$$

On the field an impressed hunting-frequency voltage Δe_{df} may exist.

(*d*). By measuring the watts of $i^*\cdot E$ and the watts and vars of $i^*\cdot\Delta E+\Delta i^*\cdot E$ as shown on Figure 2c, the steady-state torque (per phase), also the damping and synchronizing torques are found. (Note the change in sign of T_D and T_s due to that of $\Delta p\theta$.)

In per unit on Figure 2a the impressed voltages $je/2$ and $e_{df}jX_{ad}/2r_f$ are $1/\sqrt{2}$.

Appendix III. The Single-Phase Instrument-Selsyn

The Primitive Machine

Let a single-phase induction motor be considered (Figure 3a) in which the ratio of the cross-phase to the main-phase turns is a. Let it be assumed that the impedances of the two stator windings differ by $Z=R+Lp+1/pC$. Also on both stator and rotor windings let unbalanced voltages be impressed. (To simplify the equations the saliency of the stator will be neglected here, but is considered in Figure 4.)

The transient Z, G, and e of such a machine is [the primitive machine with four windings (reference 7, page 43, or reference 4)].

$$Z=\begin{array}{c|c|c|c|c|} & d_s & q_s & d_r & q_r \\ \hline d_s & r_s+L_sp & & Mp & \\ q_s & & a^2(r_s+L_sp+Z) & & aMp \\ d_r & Mp & aMp\theta & r_r+L_rp & L_rp\theta \\ q_r & -Mp\theta & aMp & -L_rp\theta & r_r+L_rp \\ \hline \end{array} \qquad (33)$$

$$G=\begin{array}{c|c|c|c|c|} & d_s & q_s & d_r & q_r \\ \hline d_r & & aM & & L_r \\ q_r & -M & & -L_r & \\ \hline \end{array} \qquad (39)$$

$$e=\begin{array}{|c|c|c|c|} d_s & q_s & d_r & q_r \\ \hline e_{ds} & e_{qs} & e_{dr} & e_{qr} \\ \hline \end{array} \qquad (40)$$

where (if $r_s=r_{sd}$ and $l_s=l_{sd}=$leakage reactance)

$$Z=R+Lp+\frac{1}{pC}=\left(\frac{r_{sq}}{a^2}-r_{sd}+\frac{R_c}{a^2}\right)+\left(\frac{l_{sq}}{a^2}-l_{sd}\right)p+\frac{1}{pa^2C} \qquad (41)$$

Reduction to Symmetrical Form

Let symmetrical components be introduced by the transformation

$$i^{ds}=(i^{fs}+i^{bs})/2$$
$$i^{qs}=-j(i^{fs}-i^{bs})/2$$
$$i^{dr}=(i^{fr}+i^{br})/2$$
$$i^{qr}=-j(i^{fr}-i^{br})/2$$

$$C=\frac{1}{2}\begin{array}{c|c|c|c|c|} & f_s & f_r & b_s & b_r \\ \hline d_s & 1 & & 1 & \\ q_s & -j/a & & j/a & \\ d_r & & 1 & & 1 \\ q_r & & -j & & j \\ \hline \end{array} \qquad (42)$$

By $C_t^*\cdot Z\cdot C$, $C_t^*\cdot G\cdot C$, and $C_t^*\cdot e$

$$Z=\frac{1}{2}\begin{array}{c|c|c|c|c|} & f_s & f_r & b_s & b_r \\ \hline f_s & r_s+L_sp+Z/2 & Mp & -Z/2 & \\ f_r & M(p-jp\theta) & r_r+L_r(p-jp\theta) & & \\ b_s & -Z/2 & & r_s+L_sp+Z/2 & Mp \\ b_r & & & M(p+jp\theta) & r_r+L_r(p+jp\theta) \\ \hline \end{array} \qquad (43)$$

$$G=\frac{1}{2}\begin{array}{c|c|c|c|c|} & f_s & f_r & b_s & b_r \\ \hline f_r & -jM & -jL_r & & \\ b_r & & & jM & jL_r \\ \hline \end{array} \qquad (44)$$

If the rotor is connected to the rotor of another Selsyn with infinite inertia, or with assumed constant speed

$$e=\begin{array}{|c|c|c|c|} f_s & f_r & b_s & b_r \\ \hline e_{fs} & (1-v)e_{fr}\epsilon^{j\delta} & e_{bs} & (1+v)e_{br}\epsilon^{-j\delta} \\ \hline \end{array} \qquad (45)$$

$$=\begin{array}{|c|c|c|c|} f_s & f_r & b_s & b_r \\ \hline e_{fs} & (1-v)e_f & e_{bs} & (1+v)e_b \\ \hline \end{array}$$

Since the reference axes are stationary the frequency of the hunting currents is $(1+h)\omega$ and $(1-h)\omega$.

1. Replacing first p by $j(1+h)\omega$ $p\theta$ by $v\omega$, and dividing Z_1' by n_1'

$$n_1'=\begin{array}{c|c|c|c|c|} & f_s & f_r & b_s & b_r \\ \hline f_s & 1+h & & & \\ f & & 1+h-v & & \\ b_s & & & 1+h & \\ b & & & & 1+h+v \\ \hline \end{array} \qquad (46)$$

$$Z_1'=\frac{1}{2}\begin{array}{c|c|c|c|c|} & f_s & f_r & b_s & b_r \\ \hline f_s & \frac{r_s}{1+h}+jX_s+\frac{Z_1}{2} & jX_m & -\frac{Z_1}{2} & \\ f_r & jX_m & \frac{r_r}{1+h-v}+jX_r & & \\ b_s & -\frac{Z_1}{2} & & \frac{r_s}{1+h}+jX_s+\frac{Z_1}{2} & jX_m \\ b_r & & & jX_m & \frac{r_r}{1+h+v}+jX_r \\ \hline \end{array} \qquad (47)$$

where the mutual impedance of the sequence axes Z_1 may have the form

$$Z_1=\frac{R}{1+h}+jX_L-\frac{jX_c}{(1+h)^2} \qquad (48)$$

2. Replacing p by $j(1-h)\omega$ and dividing Z_2' by n_2', the resultant Z_2' is the same as Z_1', except $+h$ everywhere is replaced by $-h$.

The same result is found if first p is replaced by $j(1\pm h)\omega$ then C is introduced.

The Hunting Equivalent Networks

The corresponding two equivalent networks are given in Figure 4b showing also $\Delta E_1 = \omega G' \cdot \Delta i_1$ and $\Delta E_2 = \omega G' \cdot \Delta i_2$. The saliency of the d axis is taken care of by the addition of a condenser $j(X_{mq} - X_{md})$ common to all four meshes, as in the case of the salient-pole synchronous machine, Figure 2b.

The voltages impressed on the hunting networks are by

$$\Delta e' = n'^{-1} \cdot \left(Ep - \frac{\partial e}{\partial \theta} \right) \qquad (49)$$

$$\Delta e_1' = \begin{array}{c} \\ \end{array}\begin{array}{|c|c|c|c|} \multicolumn{1}{c}{f_s} & \multicolumn{1}{c}{f_r} & \multicolumn{1}{c}{b_s} & \multicolumn{1}{c}{b_r} \\ \hline & \dfrac{j[hE_f - (1-v)e_f]}{1+k-v} & & \dfrac{j[hE_b + (1+v)e_b]}{1+h+v} \\ \hline \end{array} \qquad (50)$$

$$\Delta e_2' = \begin{array}{|c|c|c|c|} \multicolumn{1}{c}{f_s} & \multicolumn{1}{c}{f_r} & \multicolumn{1}{c}{b_s} & \multicolumn{1}{c}{b_r} \\ \hline & \dfrac{j[-hE_f - (1-v)e_f]}{1-h-v} & & \dfrac{j[-hE_b + (1+v)e_b]}{1-h+v} \\ \hline \end{array} \qquad (51)$$

The torques are the real parts of

$$\Delta T = i^* \cdot (\Delta E_1 + \Delta E_2) + (\Delta i_1 + \Delta i_2)^* \cdot E \qquad (52)$$

By measuring watts and vars, the torques per phase are found by the formulas shown in Table I.

Table I. Torque Formulas for the Single-Phase Motors of Figures 4–7

$i^f E_f^* = W'$	$i^b E^{b*} = W''$	$T = W' + W''$
$i^f \Delta E_{f1}^* = W_1 + jQ_1$ $i^f \Delta E_{f2}^* = W_2 + jQ_2$ $\Delta i^{f1} E_f^* = W_3 + jQ_3$ $\Delta i^{f2} E_f^* = W_4 + jQ_4$	$i^b \Delta E_{b1}^* = W_5 + jQ_5$ $i^b \Delta E_{b2}^* = W_6 + jQ_6$ $\Delta i^{b1} E_b^* = W_7 + jQ_7$ $\Delta i^{b2} E_b^* = W_8 + jQ_8$	
$T_a = \dfrac{W_1+W_2+W_3+W_4+W_5+W_6+W_7+W_8}{2}$	$T_D = \dfrac{(Q_2+Q_3+Q_6+Q_7) - (Q_1+Q_4+Q_5+Q_8)}{2h}$	

Special Cases

1. *The Capacitor Motor*. The rotor-impressed voltages e_{fr} and e_{br} are zero, Figure 5.

2. *Polyphase Induction Motor on Unbalanced Voltages*. $Z = 0$, Figure 6.

3. *The Single-Phase Induction Motor*. The rotor-impressed voltages are zero and $Z = \infty$, Figure 7.

4. *The Doubly Fed Induction Motor*. $Z = 0$ and $a = 1$. Also $e_{bs} = e_{br} = 0$ and the negative-sequence networks are missing, Figure 8. This case is illustrated in detail in the companion paper.[3]

5. *The Polyphase Induction Motor*. $Z = 0$, the negative-sequence networks are missing and the rotor-impressed voltage e_{fr} is also zero, Figure 9.

Appendix IV. More General Cases of Hunting

Machines With Arbitrary Reference Frames

When a relative velocity $p\theta'$ exists between the stator and rotor reference axes (as in interconnected slip ring machines such as Selsyns) the equation of voltage has an additional term $p\theta' V \cdot i$ in it, so that

$$e = (R + Lp + p\theta G + p\theta' V) \cdot i = Z \cdot i \qquad (53)$$

The equation of hunting then is

$$Z \cdot \Delta i = \left[\frac{\partial e}{\partial \theta} - (G + V) \cdot ip \right] \Delta\theta = \left(\frac{\partial e}{\partial \theta} - E'p \right) \Delta\theta \qquad (54)$$

However V is not a tensor and it cannot be physically represented.

In some cases (in machines with symmetrical structures) V may be changed into a tensor by assuming all reference axes to rotate at the same speed. But in case of interconnected synchronous machines or instrument Selsyns that assumption cannot be made.

Tensorial Form of the Hunting Equation

It is well-known that while the equation of Lagrange or its generalization, equation 53

$$e_\alpha = R_{\alpha\beta} i^\beta + a_{\alpha\beta} \frac{di^\beta}{dt} + \Gamma_{\beta\gamma,\alpha} i^\beta i^\gamma \qquad (55)$$

is a tensor equation, the equation of small oscillations (derived from it by replacing x^α by $x^\alpha + \Delta x^\alpha$ and i^α by $i^\alpha + \Delta i^\alpha$) equation 54, or

$$\Delta e_\alpha = R_{\alpha\beta} \Delta i^\beta + a_{\alpha\beta} \frac{d(\Delta i^\beta)}{dt} + \Gamma_{\beta\gamma,\alpha} \Delta i^\beta i^\gamma + \Gamma_{\beta\gamma,\alpha} i^\beta \Delta i^\gamma + \frac{\partial \Gamma_{\beta\gamma,\alpha}}{\partial x^\delta} i^\beta i^\gamma \Delta x^\delta \qquad (56)$$

is no longer a tensor equation. That is Δi^β itself is not a vector (tensor of rank one) nor can the various terms be grouped to form tensors. Hence, no physical model (equivalent circuit) can be established to represent these equations in the general case.

The tensorial form of the equation of small oscillations of dynamical systems in general and of electrical machinery in particular is[9]

$$\delta e_\alpha = R_{\alpha\beta} \delta i^\beta + a_{\alpha\beta} \frac{\delta(\delta i^\beta)}{dt} + K_{\delta\gamma\beta\alpha} i^\delta i^\beta dx^\gamma + R_{\gamma\beta\alpha} i^\beta dx^\gamma \qquad (57)$$

where $K_{\delta\gamma\beta\alpha}$ is the Riemann-Christoffel curvature tensor and δi^β represent "absolute" or "covariant" differentials. This is the hunting equation whose equivalent circuit has to be established and this is the equation that fits the hunting equivalent circuits of machines having general reference axes.

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1942 Supplement to Electrical Engineering—Transactions Section

Saturated Synchronous Machines Under Transient Conditions in the Pole Axis

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1. Introduction

THE performance of synchronous machines in steady-state operation, as well as in the transient state, is substantially determined by magnetic saturation in their iron circuits.[13,14,20] It is well-known that high initial and sustained short-circuit currents develop under widely different conditions of saturation and that large capacitive loading is unstable but for the effect of saturation.

The transition from one state of operation to another under the influence of changing saturation will be considered in this paper, and we will develop the transient phenomena from the magnetic characteristic of the machine. We fix our attention on the direct or pole axis since the fields in the quadrature axis of the rotor act essentially independently, according to Blondel's theorem.[6] The solution becomes obvious if we concentrate the saturation at the pole cores of the rotor and confine ourselves at first to symmetrical three-phase conditions in the stator and to constant speed of the machine. The analysis is simple[9] if the rotor consists of laminated cores surrounded only by exciting coils. It becomes more involved if damper windings or solid steel rotors are used having paths for eddy-current formation.

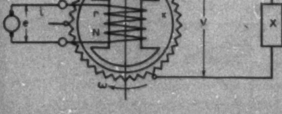

Figure 1. Constants of stator, rotor, and magnetic circuits of synchronous machine

2. Change With Time of the Main Flux

We consider first the stator circuit in Figure 1. The terminals with voltage V are loaded by an impedance X which, for simplicity, is shown with one phase only and may be purely reactive so that we have to consider only the pole axis of the machine. Consequently, the stator resistance may be neglected. Since higher harmonics are of secondary importance, we consider the phenomena of fundamental stator frequency only. With leakage reactance x, the external current I requires an internal electromotive force

$$E = V + xI = (X + x)I \qquad (1)$$

This relation holds only if the amplitude variation of the current with time is slow compared with the harmonic variation due to the frequency ω,

$$\frac{dI}{dt} \underset{<}{\leq} \omega I \qquad (2)$$

an assumption which will be verified later. With internal resistance and external active load, equation 1 remains unchanged in form, except that it then has a vector significance.

Secondly, we consider the rotor circuit in Figure 1. The external excitation voltage e is given either as constant or as a known function of time. With resistance r, number of turns N, and rotor pole flux Φ, the exciting current i in the transient state is determined by

$$e(t) = ri + N\frac{d\Phi}{dt} \qquad (3)$$

This expression can be transformed by introducing the electromotive force E rather than the flux Φ, both always being proportional,

$$\Phi/\Phi_o = E/E_o \qquad (4)$$

If we denote the rated values of flux Φ_o, stator voltage E_o and rotor voltage e_o, the last term of equation 3 equals either of the two following expressions:

$$N\frac{d\Phi}{dt} = T_m\frac{dE}{dt} = e_oT_p\frac{d(E/E_o)}{dt} \qquad (5)$$

Herein

$$T_m = N\Phi_o/E_o \qquad (6)$$

is a time constant of the complete machine which has the advantage of being constant entirely independent of the magnetic saturation, while

$$T_p = N\Phi_o/e_o \qquad (7)$$

is a time constant of the rotor poles with their field windings, which has a simpler physical significance and is constant so long as the rated rotor values remain unchanged.

The rotor circuit-equation 3 now becomes, using equation 7,

$$e(t) = ri + e_oT_p\frac{d(E/E_o)}{dt} \qquad (8)$$

It is expedient to take for Φ in equation 3 the total pole-core flux which is linked completely with the exciting winding. The electromotive force E in equation 4 corresponds to this flux, which is regarded, as usual, as the main flux of the machine to which also the magnetic characteristic is related. Hence the difference between electromotive force E and terminal voltage V is caused by the leakage fluxes of both stator and rotor and thus x in equation 1 defines the sum of stator and rotor

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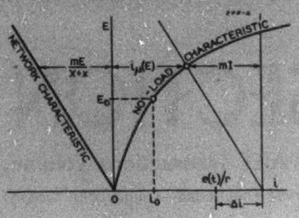

Figure 2. Internal and external excitation characteristics

leakage reactances,[7] both related to the stator circuit.

Thirdly, we consider the magnetic circuit of the machine in the direct or pole axis. With open stator terminals, the correlation of electromotive force E and exciting current i is given as "no-load characteristic" by the functions

$$E=E(i) \text{ or } i=i(E) \tag{9}$$

The rotor leakage at no load may be included herein.

With loaded machine the armature reaction of the current I causes a change of the resultant excitation. We can express this change by mI if m is a numerical factor expressing the effective turn ratio of stator and rotor windings, including the correction due to leakage and the effect of phase displacement in case of active load. If we consider the demagnetizing effect of inductive current as positive, the magnetic characteristic under load[1] is given by the function

$$E=E(i-mI) \tag{10}$$

which can be inverted to

$$i=i_\mu(E)+mI \tag{11}$$

This relation for the total exciting current i is represented by Figure 2. Herein $i_\mu(E)$ is that part of the exciting current which is needed for the magnetization of the internal main synchronous flux and mI is that part which is necessary for the magnetization of the electric stator circuit and which is transferred through the machine to the external load.

Not only the first but also the second part of the current in equation 11 is dependent solely on the electromotive force E, corresponding to the main flux Φ. We see this immediately by equation 1 since

$$I=\frac{E}{X+x} \tag{12}$$

This is represented at the left-hand side of Figure 2, and we will denote this line as the "network characteristic." With constant external and internal impedances this characteristic is a straight line through the origin, as shown in Figure 2. Its slope in terms of stator voltage and current is given merely by

$X+x$

If now we insert the stator current of equation 12 in equation 11, and the rotor current of equation 11 in equation 8, and define the rated exciting current i_o by

$$e_o=ri_o \tag{13}$$

we obtain the final relation for our problem

$$T_p\frac{d(E/E_o)}{dt}=\frac{e(t)/r-[i_\mu(E)+mE/(X+x)]}{i_o} \tag{14}$$

Thus, the change with time of the relative stator voltage E/E_o of the machine is determined by the difference of the fictitious steady-state or ohmic rotor current, namely e/r, under the impressed voltage $e(t)$, and the sum of the actual internal and external magnetizing currents necessary to produce the voltage E. This difference, indicating a current of unbalance, is denoted by Δi in Figure 2 and is obtained by drawing a parallel to the network characteristic through the working point on the no-load characteristic. If the difference Δi is positive, the voltage E increases; if Δi is negative, the voltage decreases. In both cases the rate of change is inversely proportional to the time constant T_p of the rotor.

Since the right-hand side of equation 14 depends only on the two variables t and E, we can solve this differential equation graphically for many problems. It is convenient to write equation 14 in a purely numerical form, namely

$$\frac{d(E/E_o)}{d(t/T_p)}=\frac{e(t)}{e_o}-\frac{\Sigma i(E)}{i_o} \tag{15}$$

where the last term on the right-hand side represents the excitation current of the entire internal plus external magnetic characteristic, as shown in Figure 3a. In Figure 3b and 3c there are derived by use of equation 15 the changes with time of voltage and exciting current when the load current of the generator is suddenly decreased. Under the influence of a voltage regulator a series resistance in the shunt excitation of the exciter may be switched on and off when the voltage E passes through the rated value. The voltage curve $e(t)$ of the exciter is given in Figure 3c, and the difference at every instant between this voltage and the total exciting current, taken from the characteristic in Figure 3a, both given in relative values, determines the rate of change of the voltage E, as indicated in Figure 3b. Step by step, the complete set of curves can be derived in this way, and experiments show that such curves agree well with actual oscillograms.[5,8]

For many important phenomena, the excitation voltage e remains constant during the transient process and this simplifies the problem materially. The exciting current immediately before the instant of switching is

$$i_1=e_1/r \tag{16}$$

e_1 denoting the constant rotor voltage. The first term on the right-hand side of equations 14 and 15 now is independent of time and this relation can be written

$$T_p\frac{(dE/E_o)}{dt}=\frac{i_1}{i_o}-\frac{\Sigma i(E)}{i_o}=\frac{\Delta i(E)}{i_o} \tag{17}$$

where the difference current Δi is now dependent on the voltage E only. From Figure 4a we see that this value

$$\Delta i=i_1-i_\mu-mI \tag{18}$$

is given by the difference shown shaded between the internal magnetic characteristic of the machine and the external network characteristic, drawn backwards

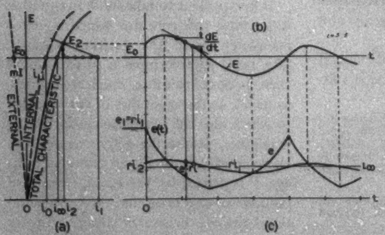

Figure 3. Point-by-point derivation of voltage- and current-time curves after sudden drop of load

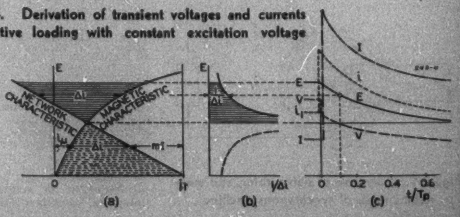

Figure 4. Derivation of transient voltages and currents at inductive loading with constant excitation voltage

from the original exciting current i_1. For every voltage E the change with time of the voltage is determined, therefore, by the horizontal distance between the two characteristics, and the entire E curve can easily be derived graphically. We can integrate equation 17 rigorously by separation of the variables and obtain the time elapsed since the instant of switching

$$t = T_p \int \frac{d(E/E_o)}{\Delta i/i_o} \qquad (19)$$

This constitutes a simple quadrature and Figure 4b and c show how the E curve plotted against time can be derived by graphical integration of an auxiliary curve $1/\Delta i$ dependent on E.

The electromotive force E departs from the initial value which is determined by the previous operation of the machine, decreases at a rate determined by the value of Δi and approaches asymptotically the intersection of the internal and external characteristics, indicating a state of equilibrium. This new steady-state value may be approached from above or from below, depending upon the initial magnitude of the voltage E.

3. Transient Voltages and Currents

The other parameters of operation, as terminal voltage, exciting current, and stator current, can easily be determined graphically. According to equation 1, the terminal voltage V is the difference between electromotive force E and leakage reactance voltage xI. If, therefore, we draw in Figure 5 through the steady-state exciting current i_1 a straight line representing the leakage characteristic of the synchronous machine, with slope x rather than $X+x$ as for the network characteristic, this line subdivides the entire voltage E into two parts, namely, the terminal voltage V and the leakage voltage xI. Thus we can take the value of V for every value of E and transfer it to the time diagram, Figure 4c. While the electromotive force E is continuous at the instant of switching, the terminal voltage V jumps by an amount equal to the change of leakage voltage at the instant of switching.

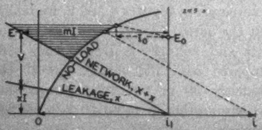

Figure 5. Three characteristics which are constant during inductive transient performance

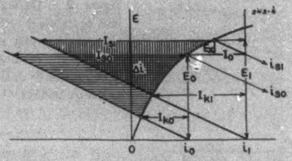

Figure 6. Unbalance of currents at sudden short circuit under two previous machine conditions

The value mI for the stator current I is given in Figure 5 by the horizontal distance between the characteristic point at the value E on the network characteristic and the vertical through the exciting current i_1. This follows directly from the definition of the network characteristic. Thus the stator current I can also be plotted in the time diagram, Figure 4c. The exciting current given by equation 11 is, according to Figure 5, the value cut off the i axis at a given instant by a line through the E point at the no-load characteristic and parallel to the network characteristic. It also is transferred to Figure 4c. Stator current and rotor current behave discontinuously at the instant of switching and jump suddenly to their new values, decreasing subsequently with time to their steady-state values, which for the exciting current coincides with the magnitude prior to the switching process.

In every case it is easy to determine the correct scale for the stator current. We need only to consider the Potier triangle in Figure 5, which by its magnitude for zero power factor gives a direct measure of the rated stator current. Thus we can measure I directly, rather than mI in the rotor scale.

A significant case of operation is the sudden short circuit of a synchronous machine at its terminals. Since now there is no external reactance X, the network characteristic becomes identical with the leakage characteristic and, therefore, drops in position. This is shown in Figure 6 for two different prior exciting currents, corresponding to no-load and to full-reactive-load conditions. The equilibrium of the currents in the machine is now heavily disturbed, and thus a large difference current Δi occurs. The voltage E, therefore, decreases rapidly, and very large currents are built up, constituting in the stator an alternating current I and in the rotor a direct current i, both decreasing continuously from their initial values to the final sustained values. Hence we have developed a method for determining numerically the change with time of these currents with any given saturation of the synchronous machine. Figure 6 shows that the initial short-circuit stator currents I_s for prior no-load and full-load conditions differ only slightly, this difference being equal to the magnitude of the prior load current, though the sustained currents I_k are widely different. In any case, the value of the initial alternating short-circuit current, given by the similarity of the triangles, is

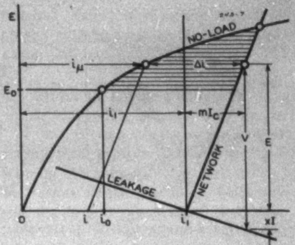

Figure 7. Three characteristics which are constant during capacitive transient performance

$$I_s/I_o = E/E_x \qquad (20)$$

where E denotes the electromotive force at the instant of short circuit and E_x the total leakage voltage of the machine at rated current I_o.

Another interesting case is the capacitive loading of a synchronous generator. The load current now is, by equation 1

$$I = \frac{E}{-1/\omega C + x} = \frac{\omega C}{1 - \omega C x} E = -I_c \qquad (21)$$

and thus changes sign. The network characteristic therefore must be drawn from the exciting current i_1 toward the right-hand side, as in Figure 7, the slope being determined by the coefficient of E in equation 21. The increment of the capacitive current due to the combined action of external capacitive reactance and internal leakage inductance is given by the denominator of this coefficient. Resonance would be approached only for very large values of the capacitance. Equation 18 can now be written in the form

$$\Delta i = i_1 - i_\mu + mI_c \qquad (22)$$

and we see from Figure 7 that Δi is again the horizontal difference between the no-load characteristic and the capacitive characteristic of the stator network. However, since Δi has changed its sign compared with the inductive loading of Figure 4, the voltage now increases with time toward the final value, again deter-

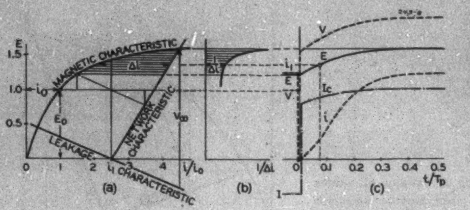

Figure 8. Derivation of transient voltages and currents at capacitive loading with constant excitation voltage

mined by the intersection of the two characteristics.

The terminal voltage V is always the vertical distance between network and leakage characteristics. If we lengthen the leakage characteristic in Figure 7 beyond the current i_1, we see that V becomes larger than the electromotive force E by reason of the inductive voltage rise produced by the capacitive current. A parallel line to the network characteristic through the E point on the no-load characteristic cuts off on the abscissa the magnitude of the exciting current i, as shown in Figure 7. The transient exciting current here is temporarily fairly small and even may become negative.

Figure 8 represents the sudden capacitive loading of a synchronous generator, previously under full inductive load, when the capacitance is so chosen that it would give full load under normal voltage. By drawing the inverted difference current $1/\Delta i$ and integrating this, we obtain in Figure 8c the change of voltage E with time, according to equation 19, and by graphical correlation we can add the other curves for terminal voltage V, stator current I_c and exciting current i. At the instant of switching all magnitudes, except the electromotive force E, jump to their new transient values. The exciting current returns finally to its original value i_1, and electromotive force, stator current and, particularly, terminal voltage attain high magnitudes.

We now can state the general rule for any transient change in synchronous machines with constant excitation voltage. Up to the initiation of the switching process the machine may have worked, as in Figure 9, with an exciting current i_1 and an electromotive force E, both determined by the intersection of the no-load machine characteristic with the prior load network characteristic. When the network characteristic suddenly changes by a definite amount, turning to the left-hand side with increase of inductive load or to the right-hand side with decrease of inductive load or increase of capacitive load, the working point of the machine moves along the magnetic characteristic toward the new intersection at a rate given by equation 17 or 19, which is proportional to the current difference Δi of the shaded areas in Figure 9.

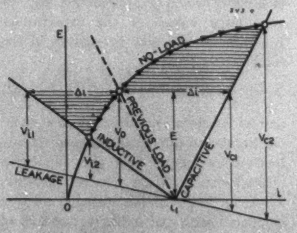

Figure 9. Unbalance of currents at sudden change of general reactive load

The phenomena occurring with removal of load of a synchronous machine are also easy to describe. If, for example, the sustained terminal short-circuit current of a generator is suddenly interrupted, as in Figure 10, the exciting current jumps temporarily to a very small value, i' or i'' according as the load changes to zero or to a finite value. The electromotive force E increases with a rate determined by the width of the shaded areas, and the momentary values of terminal voltage V and stator current I for any instant can easily be taken from the diagram by projecting from the load characteristic to the no-load and short-circuit lines. If, on the other hand, a capacitive load is switched off[4,12] as in Figure 11, the exciting current jumps temporarily to the larger value i' or i'', according to the final-load or no-load condition. Stator voltages and current can also be taken from the diagram, using the shaded areas and following the same scheme as before.

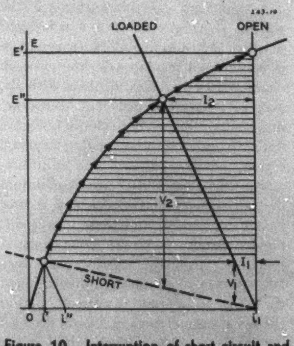

Figure 10. Interruption of short circuit and recovery toward loaded or open-circuited condition

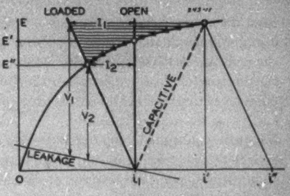

Figure 11. Interruption of capacitive load and recovery toward loaded or open-circuited condition

The only magnitude of our main equations 17 and 19 which is not contained in the diagram of characteristics is the time constant T_p of the rotor pole flux. Calculations according to equation 7 and oscillographic experiments have shown that for machines of the usual design for 50–60 cycles per second, with no additional resistance in the excitation circuit, the time constant T_p varies mainly with the synchronous power generated by each pole. It is:

for 100 1,000 10,000 kva per pole

in the order of

$T_p =$ 3 6 12 seconds

With numerical values of the integral of equation 19 such as are given on the axis of abscissae in Figures 4 and 8, we realize that the actual change with time of currents and voltages is always very slow compared with the change due to the frequency 50 or 60 cycles per second. Our assumption in equation 2 is verified therefore.

However, this is true only during the transient state following the instant of switching, but is not true for the time $t = 0$ at which the switching occurs. Our curves for the stator currents and voltages represent only the amplitudes of the harmonically varying magnitudes, so that a jump of these amplitudes at $t = 0$ does not mean necessarily an actual discontinuity but merely a rapid variation compared to the slower succeeding change. It is well-known that other transient currents may appear which prevent a discontinuous transition between the values immediately before and immediately after $t = 0$. These additional transient currents flow without impressed voltage, suffer a rapid decay, and bridge the jump of the amplitudes only at the instant of switching. Their initial values, therefore, are given directly by the jumps of the currents and voltages in our time diagrams.

If the synchronous machine is loaded by self-inductance, either external or only internal, the superposed transient current is a decaying direct current, its time constant being that of the stator circuit. If, however, the load consists of capacitance, the superposed current is a damped oscillating current, the natural frequency being determined by capacitance and self-inductance of the machine and the load. It is well-known that the actual currents or voltages, by the additional effects of these free intermediate currents, may rise temporarily to about double the value of the original discontinuous magnitudes.

The rate of change of any curve can be expressed significantly by the value of the subtangent S. This is shown in Figure 12b with curve E which is derived from the characteristics in Figure 12a of the magnetic and electric circuits of a synchronous machine. By using equation 17 we can express the subtangent, as cut off on the asymptotic axis,

$$S=\frac{\Delta E}{dE/dt}=T_p\frac{\Delta E/E_o}{\Delta i/i_o} \tag{23}$$

if we denote by ΔE the difference of the momentary and final voltages, corresponding to the current difference Δi. Hence the ratio of the subtangent to the time constant of our problem is given by the ratio of the height to the width of the shaded characteristic area in Figure 12a, both measured in relative values.

Let us consider two extreme examples with different asymptotic points of equilibrium. With no load of a nonsaturated machine, we see from Figure 13a that

$$\left.\begin{aligned}&\Delta E/\Delta i=E_o/i_o\\&\text{hence}\quad S_o=T_p\end{aligned}\right\} \tag{24}$$

or the subtangent equals the time constant. With saturation, corresponding to the dashed curve in Figure 13a, ΔE for the same Δi is smaller than before and, therefore, $S<T_p$.

With terminal short circuit of a nonsaturated machine, Figure 13b shows, with I_s for the sudden and I_k for the sustained short-circuit current, that

$$\left.\begin{aligned}&\Delta i=mI_s,\quad \Delta E=E_o\frac{mI_k}{i_o}\\&\text{hence}\quad S_k=T_p\frac{I_k}{I_s}\end{aligned}\right\} \tag{25}$$

and the subtangent is a fraction only of the rotor-pole time constant T_p. This means that the equivalent self-inductance of the field winding is reduced by the reaction of the short-circuited armature. Thus the decay of the electromotive force and of the main flux of a machine under short-circuit conditions is more rapid than under no-load conditions in the ratio of sustained to sudden short-circuit current. With saturation, corresponding to the dashed curve of Figure 13b, Δi becomes larger for the same ΔE, and therefore S is smaller than before.

So we see that the subtangent S of the E curve is constant only for nonsaturated machines, and therefore only these machines can show an exponential decay of the voltages and also of the load- and short-circuit currents. Actual saturated machines, however, have a subtangent S which, corresponding to equation 23 and Figure 12, may be small during the initial stages and may increase toward a constant value at the final stages only, where the magnetic characteristic approaches the steady-state point linearly in the direction of its tangent. With capacitive load this rule is inverted, the subtangent decreases with higher saturation. All influences which by smaller armature reaction elevate the network characteristic, such as ohmic resistance or one- or two-terminal loading, increase the magnitude of the subtangent toward the no-load value of equation 24.

4. Effect of Damper Circuits

If the poles of a synchronous generator are equipped with damper circuits, these form for the most part a squirrel cage at the surface of the pole faces, both with salient-pole and cylindrical rotors. The total flux in the damper winding thus is identical with the flux entering the stator surface, if we neglect the very small circumferential flux in the air gap, including the damper slot flux. Between damper and field windings, however, a circumferential flux can pass to the adjacent poles, as in Figure 14. This constitutes the rotor leakage flux, in the transient part of which we are interested. A lumped damper circuit rather than a distributed winding is shown in Figure 14 in order to simplify the problem with regard to the direct or pole axis.

We denote by σ the leakage coefficient between damper and excitation windings due to the rotor leakage flux, by T_d the magnetic time constant of the damper winding, given as quotient of self-inductance and resistance, while T_p is the time constant of the exciting pole winding as before. It is well-known that, without regard to saturation, two exponential transient currents may flow in the two magnetically coupled windings, the time constants T of which are given as roots of the quadratic equation[2]

$$T^2-T(T_p+T_d)+\sigma T_pT_d=0 \tag{26}$$

Hence for small leakage, as in actual machines, the time constants are very nearly

$$T_1=T_p+T_d;\quad T_2=\frac{\sigma}{1/T_p+1/T_d}\cong\sigma T_d \tag{27}$$

where the last term on the right-hand side is a further approximation for T_2 for relatively small T_d. The primary time con-

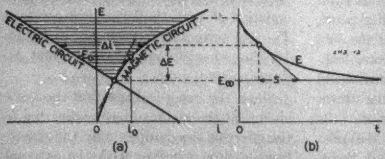

Figure 12. Subtangent of the voltage curve defining the character of change with time

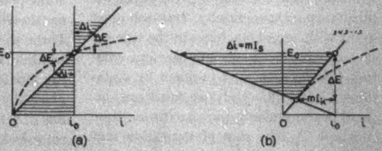

Figure 13 (right). No-load and short-circuit conditions of nonsaturated and saturated machines

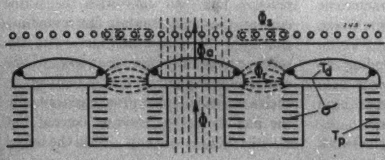

Figure 14 (lower left). Damper circuits and rotor leakage in synchronous machine

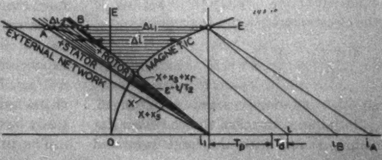

Figure 15 (right). Behavior of slow-transient main flux and rapid-transient rotor-leakage flux at inductive loading

stant T_1, given by the sum of T_p and T_d, represents the change with time of the main flux, and the secondary time constant, T_2, given by the product of two small quantities, represents the change with time of the leakage flux. Thus we can distinguish between a slow-transient main flux and a rapid-transient leakage flux. To both fluxes are correlated the respective voltages and currents in the windings.

An actual medium-sized machine may have an excitation time constant of the poles $T_p=5$ seconds, a damper time constant $T_d=1$ second, and a leakage coefficient between both rotor windings $\sigma=$ 15 per cent. Then we have the two resultant time constants

$$T_1=5+1=6 \text{ seconds}$$

$$T_2=0.15\,\frac{5\cdot 1}{5+1}=0.125 \text{ second}$$

T_2 being only two per cent of T_1. For current of 60 cycles per second the exponential damping for half a cycle is

$$\epsilon^{-\frac{t}{T_1}}=0.999;\quad \epsilon^{-\frac{t}{T_2}}=0.933$$

The rapid-transient rotor leakage flux flows for the most part in air, as seen by Figure 14. Hence this flux, constituting a free magnetic field which acts in addition to the main pole flux, is proportional or nearly so to its exciting currents. Therefore, it will be only slightly influenced by saturation and thus will change exponentially with time in actual machines. The leakage time constant T_2 is, therefore, a true or nearly true exponential time constant. It may vary, however, to some extent with different loading of the stator, caused by armature reaction on the damper circuit.[21]

The slow-transient main flux, on the other hand, flows through the armature and poles of the machine and is, therefore, greatly influenced by saturation of these steel parts. Thus, we cannot expect that the main time constant T_1 will affect the phenomena exponentially, but we must use it with actual machines in the way expressed graphically in our previous solution for the saturated problem by equations 17 and 19. Only we must use, instead of the previous pure excitation time constant T_p, the sum of excitation and damper time constants given by the first equation 27, T_1. Hence the main flux, or the electromotive force, is given by

$$T_1\,\frac{d(E/E_0)}{dt}=\frac{\Delta i}{i_0} \tag{28}$$

all other notations remaining unchanged.

Both fluxes, together, the slow-transient main-pole flux with the large saturated time constant T_1 and the rapid-transient rotor-leakage flux with the small exponential time constant T_2, constitute the armature flux entering the surface of the stator and producing in combination with the stator-leakage flux the terminal voltage and the stator current. At the instant of switching, the field winding keeps the pole flux constant, the damper keeps the air-gap armature flux constant, and thus only the stator-leakage flux can change instantaneously.

The initial values of all magnitudes can be determined by a diagram of characteristics as in Figure 15, which shows the case of sudden inductive loading of the synchronous machine. If no damper were present, there would be effective from the beginning the total network characteristic, formed by the reactances $X+x_s+x_r$, the last two terms denoting the leakage reactances of stator and rotor separately. Point B on this characteristic then would represent the current and would move down at a rate given by our previous considerations. With damper winding at the pole surfaces, however, not only the main-pole flux but also the rotor-leakage flux remains constant during the instant of switching, since the leakage flux is linked with the damper winding. Only the external field and the stator-leakage flux can vary, therefore, with the stator current, and this is represented by the line $X+x_s$ in Figure 15. This line intersects with the voltage of the constant-pole flux at point A, and this point is, therefore, the actual starting point. If the damper were as strong as the field winding, it would sustain the rotor-leakage flux for some time, and point A would gradually move down along the stator line $X+x_s$. Actually, however, the rotor-leakage flux varies with the relatively small time constant T_2 and, therefore, the effective network characteristic turns rapidly from the initial line $X+x_s$ to the final position $X+x_s+x_r$ in an exponential manner, corresponding to ϵ^{-t/T_2}. If T_2 is known, the intermediate characteristics for successive times can easily be drawn into the diagram.

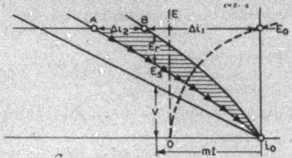

Figure 16. Effect of magnetic saturation in path of leakage flux

The turning of this effective network characteristic corresponds to the rapid decay of the transient rotor-leakage flux. During this time, which is usually of the order of a tenth of a second, the main flux and its electromotive force E, also shown by Figure 15, have just started to move down over the shaded area at a rate given by the time constant T_1 and the current difference Δi. The smaller the external load, the steeper is the external characteristic X and the smaller is the current difference Δi to which, according to equation 28, the change of voltage with time is proportional. Thus, with known time constants and known characteristics, it is easy to trace the intermediate curve between stator and rotor lines in Figure 15 on which the point A gradually moves down toward the final intersection of network and magnetic characteristics.

If the leakage circuits of stator and rotor are saturated in any way,[15, 16] we can include this effect in our considerations. Instead of straight lines, we have merely to draw curved additional stator- and rotor-leakage lines, as in Figure 16. The total network characteristic now is composed of the external reactive voltage V and the internal leakage voltages E_s and E_r, both depending on the current I with different effects of saturation. Corresponding to our previous consideration of saturated transients, Δi_2 in Figure 16 represents a current difference sustained by the damper circuit, which for some time keeps alive the rotor-leakage flux. The saturated rapid-transient variation therefore is given, corresponding to equation 17, by

$$T_2\,\frac{d(E/E_0)}{dt}=\frac{\Delta i_2}{i_0} \tag{29}$$

which is easy to evaluate graphically. According to this relation point A moves rapidly down to zero in a nonexponential manner, and the additional damper current Δi_2 can be determined for any time t. Meanwhile the pole flux in the main magnetic circuit decreases slowly according to equation 28 with the large time constant T_1. Its current difference Δi_1, in Figure 16, thus changes only slightly while Δi_2 drops to zero.

Since the rotor-leakage flux does not change at the instant of switching due to the effect of the damper, only the stator-leakage flux, together with the external reactive flux, can vary with the stator current, the total armature flux remaining constant. As shown in Figure 17a, the stator current I jumps, therefore, to the characteristic point A and is built up to such a magnitude that it balances not only the increase of the exciting current but also the damper current. Thus the initial value I_A of the stator current is given in Figure 17a by the horizontal distance between the previous exciting current i_1 and the point A. The terminal voltage V is always given by the height of the external characteristic X in Figure 17a under the working point on the actual network characteristic. At the initial moment, V_A is found as the section under point A, the difference from the electromotive force E consisting in the stator-leakage voltage only, since the rotor-leakage flux does not vary at that instant. After some time, however, when the effective network characteristic has turned to its final position, the difference $E-V$ is given always by the combined stator- and rotor-leakage voltage. The slow transient part of the terminal voltage originates at the value V_B under the point B, the difference V_A-V_B being equal to the rotor-leakage voltage, and decreases toward the steady-state value V_∞.

In Figure 17b the slow transient drop of the electromotive force E and the slow and rapid transient decay of the terminal voltage V are shown following sudden loading of the machine. The electromotive force E, corresponding to the rotor-pole flux, decreases continuously from the no-load value, finally attaining the steady-state value at the intersection of both main characteristics. The terminal voltage, V, however, jumps from the no-load value, identical with the electromotive force, by an amount caused by the suddenly appearing stator-leakage drop and reaching instantaneously V_A. The rotor-leakage drop, on the other hand, does not occur suddenly but builds up gradually with the time constant T_2, and thus effects a rapid decrease of the terminal voltage to the asymptotic curve beginning with V_B and caused by the decay of the main-pole flux. The stator current follows a similar curve taken from the horizontal distances in Figure 17a and also shown in Figure 17b.

Since at the first instant the damper acts merely as a supplement to the field winding, excluding any effect of the rotor leakage, we obtain the total rotor current at the instant of switching by drawing through the working point on the magnetic characteristic, as in Figure 15, a parallel to the stator line $X+x_s$. Later, however, after the decay of the rapid-transient magnitudes, we must draw the parallel to the total network characteristic $X+x_s+x_r$. Thus the total rotor current starts with a high value i_A and approaches rapidly the slow transient current decreasing from the initial value i_B and finally reattaining the previous current i_1. Hence by the presence of the damper winding, stator current, as well as rotor current, acquires a superposed rapid-transient peak which decays exponentially with the time constant T_2, while the excitation winding causes a subsequent slow-transient decrease determined in its shape by the shaded area. The main-pole flux and the electromotive force E decrease continuously without superposed initial peak.

The total transient rotor current consists of exciting current and damper current. During the slow-transient period both are produced by the same decreasing main flux, therefore inducing the same voltage per turn in the respective windings. The subdivision of the slow-transient current into its components is, therefore, determined by the resistances or by the time constants of the exciting winding T_p and the damper winding T_d, as shown in Figure 15. The rapid-transient supplementary current Δi_2, on the other hand, induced by the decaying rotor-leakage flux, flows entirely in the damper circuit. It is understood that the damper current is measured here in the scale of the field current and may be converted to its proper value by the turn ratio.

5. Initial Conditions Under Load

If the synchronous machine at the prior condition is not open-circuited but is under load, the initial fluxes in stator and rotor are not equal. In Figure 18 the Potier-Sumec load triangle, or rather quadrangle,[8] is shown in which point a marks the magnitude of the stator flux, and b that of the rotor flux, which is larger, because of the increased rotor leakage flux under load. Figure 18 represents the phenomena occurring under a sudden short circuit near the machine terminals, so that the external characteristic vanishes completely, and only the rotor- and stator-leakage characteristics remain, both of the same order of magnitude. Without a damper, the pole flux corresponding to point b would remain constant at the instant of switching, and the current difference between B and b would move down the shaded area, according to equation 17.

This simple process is altered in two respects by the presence of a damper. First, the saturated time constant for the rotor-pole flux is increased to the value of the first equation 27, and thus the current difference Δi moves more slowly according to equation 28. Second, not only the pole flux but also the rotor-leakage flux, and therefore the total armature flux, remain constant at the instant of switching. Since this stator flux for the prior load condition is given by the point a in Figure 18, the corresponding point on the stator leakage characteristic up to which the flux can vary instantaneously is A, and, therefore, the stator current jumps from the prior value I_0 to the large value I_A. This is the instantaneous value of the stator short-circuit current and its magnitude is

$$I_A=\frac{E_0+E_s}{x_s}=\frac{E_0}{x_s}+I_0 \tag{30}$$

It exceeds the no-load instantaneous current merely by the value of the prior load current, as was also the case without damper action.

With the decay of the rapid-transient rotor-leakage flux, the characteristic line x_s turns, with an exponential time constant T_2, to the total leakage characteristic x_s+x_r. Meanwhile the main-pole flux with the electromotive force E moves down, producing a slow-transient stator current beginning at point B with the value

$$I_B=\frac{E_0+E_s+E_r}{x_s+x_r}=\frac{E_0}{x_s+x_r}+I_0 \tag{31}$$

Thus both the initial short-circuit currents are determined mainly by the prior terminal voltage E_0 and the stator- and rotor-leakage reactances.

The peak of the rapid-transient current, on the initial value of the slow-transient current, Figure 17, is given by the difference of equations 30 and 31 and is large with terminal short circuit. With distant short circuits, however, where the denominators of equations 30 and 31 contain, in addition, the external impedance, the peak is, without prior load,

$$\frac{I_A-I_B}{I_B}=\frac{x_r}{X+x_s} \tag{32}$$

a ratio indicated in Figure 17, and this excess value becomes less significant the larger the external impedance.

When evaluating the stator currents from such diagrams, it should be noted that the scales for the two currents I_A and I_B are slightly different, due to the fact that the ratio of stator current to rotor current is given mainly by the turn ratio, but is also slightly dependent on the leakages. I_A, however, is determined only by the stator leakage, while I_B is determined by both stator and rotor leakages.

We can now abandon our conception of a lumped damper winding as in Figure 14, which gives a fixed value of secondary time constant. It is well-known that multiple circuits, extended in space, as they are used with squirrel-cage dampers and also with solid cylindrical rotors, have not one single time constant but a series of

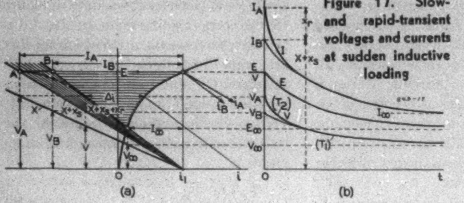

Figure 17. Slow- and rapid-transient voltages and currents at sudden inductive loading

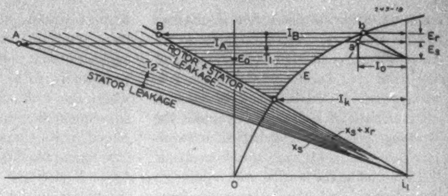

Figure 18. Influence of stator and rotor leakage at sudden short circuit of previously loaded synchronous machine with damper

exponential time constants, each belonging merely to a partial amplitude of the current. This means that the flux through distributed dampers does not decay exponentially but according to a more complicated time function, beginning rapidly and ending slowly. Thus we have to turn the effective leakage characteristic from the stator line to the rotor line in Figures 15–18, not exponentially, but quickly at first and more slowly finally, approaching the speed of the lowest rapid-transient time constant. Hence we see that the starting and ending lines of the rapid-transient period always go through the points *A* and *B*, and that only slight alterations result in the intermediate position of the effective characteristic. The initial and final values of the rapid- and of the slow-transient voltages and currents also remain unchanged.

It may be noted that the slow-transient and rapid-transient phenomena of this investigation are not identical with the transient and subtransient phenomena commonly used.[10,11,18] Figure 19 shows for a sudden short circuit the difference between the two conceptions. The "subtransient" current is usually defined as the difference between the actual change of the current with time and the "transient" exponential asymptote, which has a time constant equal to the longest subtangent. This latter curve, drawn backwards as dashed in Figure 19, lies considerably deeper than the slow-transient curve which has a smaller subtangent in the beginning due to the magnetic saturation. The "transient reactance," defined by the start I' of the slowest exponential asymptote, is therefore with saturated machines materially greater than the sum of stator- and rotor-leakage reactances, the sum giving the beginning I_B.

Let us consider some further cases which are of interest in the operation of synchronous machines. Figure 20 shows the phenomena at the clearing of a short circuit near the terminals after which the machine again supplies a certain load. The diagram contains the stator- and rotor-leakage characteristics for the prior short-circuit operation, and the network characteristic for the final operation, including stator and rotor leakages separately. At the instant of interrupting the short circuit, the rotor-pole flux starts on the magnetic characteristic at a point *b* given by stator-plus rotor-leakage characteristic. The pole flux and electromotive force change, according to the shaded area, with the difference current Δi between magnetic characteristic and total network characteristic. This construction is the same as would be used without damper action except that the time constant is somewhat larger, namely T_1. This gives the initial slow-transient terminal voltage V_B and the subsequent change with time, as shown in Figure 20b, up to the final voltage V_∞.

However, the actual armature flux at the instant of switching is given by the stator-leakage flux only, corresponding to point *a*, and this leakage flux alone can jump at the instant of switching. Thus only the stator-leakage characteristic jumps suddenly to the network stator line $X+x_s$ through point *A* in Figure 20a. The initial terminal voltage immediately after interruption of the short circuit is, therefore, given by V_A as the vertical difference between the two stator lines, under load and under short circuit. The difference between the two voltages V_B and V_A, corresponding to the initial rotor-leakage flux, decays rapidly with the small time constant T_2, as shown in Figure 20b. Hence, the terminal voltage, being zero during the time of short circuit, jumps with the interruption instantaneously to a value determined by the prior stator-leakage drop, then increases rapidly due to the vanishing rotor-leakage flux sustained for a short time by the damper winding,[17] and finally creeps slowly upward due to the reappearing main-pole flux of the machine.

This change with time of the recovery voltage is important in the operation of circuit breakers interrupting a short-circuit current. We see that this voltage consists of three parts, the rate of ascent of which for full interruption of the current we will compute. The first step is the instantaneous appearance of the prior stator-leakage voltage V_s at the terminals. The rate of increase is V_s/O, and this indicates that an overshooting of voltage can occur if capacitance is present in the circuit. The second step is constituted by the prior rotor leakage voltage V_r and its rate of change is determined by the small time constant as V_r/T_2. The third step, following the magnetic saturation curve with the time constant T_1, is given by equation 28. Thus the total initial rate of increase of the recovery voltage is

$$\left(\frac{dV}{dt}\right)_O=\frac{V_s}{O}+\frac{V_r}{T_2}+\frac{E_0}{T_1}\frac{\Delta i}{i_0} \tag{33}$$

Usually the second term is five to ten times as large as the third term, and, therefore, the subsequent increase of the recovery voltage is determined mainly by the rotor-leakage voltage and the rotor-leakage time constant.

The stator current flowing into the load, given by the horizontal distance between the network characteristic and the vertical through the steady-state excitation, starts with relatively small magnitudes I_A and I_B and increases slowly to its final value. The transient rotor current is again cut off on the axis of abscissae by a parallel to the final network characteristic and divides into a larger exciting current and a smaller damper current in the ratio of the time constants T_p and T_d.

The sudden capacitive loading of a synchronous generator is shown in the diagram Figure 21. An extension of our previous Figure 8, this figure shows the transition from full inductive load to full capacitive load under the influence of magnetic

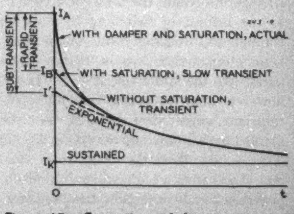

Figure 19. Comparison of slow-transient and rapid-transient phenomena with division into transient and subtransient curves

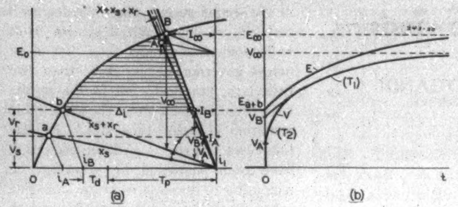

Figure 20. Interruption of terminal short circuit of synchronous machine with damper

saturation and damper circuits. The slow-transient voltage is determined by the large time constant T_1 and the shaded current difference, Δi beginning between the complete network characteristics under full inductive and full capacitive load. Superposed are the rapid-transient magnitudes given by the turning of the stator line to the rotor line of the final capacitive characteristic. The terminal voltage starts with a jump from E_0 to the vertical height V_A through point A of the subsequent stator line, indicating unchanged stator flux, and approaches the rotor line through point B at constant rotor flux.

In Figure 21b the change with time of stator voltage and rotor current is represented. The voltage increase due to stator leakage is instantaneous, the increase due to rotor leakage is rapid, and the increase due to the main flux is relatively slow. The exciting current jumps to a small value and regains its prior value gradually. The stator current also jumps suddenly from the inductive to the capacitive direction and increases further to its final value, I_∞, given by the intersection of the characteristics. Actually, all discontinuities of voltage and current are smoothed by a superposed transient oscillation between the capacitance and the inductance. Since the natural period of these oscillations is always small compared even with the time constant of the damper winding, only the stator leakage is effective and determines the natural frequency.

We have assumed in the beginning that the magnetic saturation is concentrated in the rotor poles, a condition under which the Potier-Sumec quadrangle of Figure 18, for example, is a straight-line figure. If, however, the stator is also saturated in yoke and teeth, the increased rotor-leakage flux needs additional excitation by rotor saturation alone, and thus point a approaches the main magnetic characteristic. In every case, characteristics for the total stator flux and total rotor flux can be plotted separately[19] and used in an equivalent manner, as the corners of the straight-line quadrangle have been used for rotor-pole saturation only.

Although for the sake of simplicity our deductions have been confined to symmetrical, three-phase, reactive load having a straight-line external characteristic, there is no restriction in principle against using our graphical method for other conditions of load. Any active load also gives a definite correlation of voltage and stator current, except that we must consider, with respect to the armature reaction within the machine, the internal reactive component only. This can be done by methods well-known in the two-reaction theory of synchronous machines. With any kind of constant-impedance load, the external characteristic, and, therefore, the total network characteristic, including leakages, remains linear. Even unbalanced loads, as under line-to-neutral or line-to-line short-circuit conditions, can be treated in the same way except that only their positive-sequence component enters the graphical analysis. The remaining negative-sequence and zero-sequence components, on the other hand, do not interact materially with the synchronous fields of the machine if damper circuits are present.

If, finally, the load consists of other synchronous or induction motors which generate a counterelectromotive force and, therefore, prevent the external characteristic from passing through the origin, or if the load consists of transformers with magnetizing currents increasing rapidly with the voltage, the network characteristic may become flat-topped or markedly curved. Since, however, the linearity of the network characteristic does not play any part in our derivations, we see that our general method of solution remains unchanged even in such more complicated cases.

6. Summary

By reducing to the same parameter with saturation the voltage-current equations for stator, rotor, and magnetic circuits of synchronous machines, a differential equation in time for the main-pole flux and the induced electromotive force

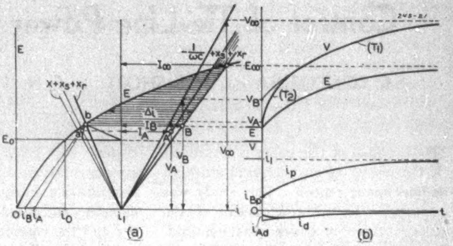

Figure 21. Capacitive loading of synchronous machine with damper

is derived, a relation easy to evaluate graphically. The difference of current between the magnetic characteristic of the machine and the electric characteristic of the stator circuit determines the rate of change of the electromotive force from which all the other slow-transient magnitudes can be derived.

For constant excitation voltage, a closed solution for the variation of flux and electromotive force is given, valid for any sudden change of load, including short-circuit current formation or capacitive superexcitation. The combined effect of damper circuits and rotor leakage causes a superposed rapid-transient variation manifested by an additional current peak at sudden short circuits and by a rapid initial voltage rise at sudden interruptions of the circuit.

The method of solution is in principle independent of the distribution of the saturation on rotor and stator, as well as of the character of loading of the machine, be it by constant or variable impedance, by symmetrical or unbalanced currents, by active or reactive power. The common subdivision into transient and subtransient phenomena is not identical with the separation into slow- and rapid-transient effects, the physical significance of which is derived in this paper.

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6. An Extension of Blondel's Two-Reaction

Control of Tie-Line Power Swings

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THE object of this paper is to present the results of an analytical study of tie-line power control. This study was made as a logical development of the general subject of power system control. Previous work[1,2] has indicated the general requirements of the prime-mover speed governors and the desirability of supplementary control to insure proper load division, frequency, and time. The general requirements of supplementary controls are given in the companion paper[3] by Crary and McClure. In most cases these controls are satisfactorily obtained as rather slow corrective adjustments to the speed-governor mechanisms, but for certain types of load a more active tie-line load controller, which will tend to suppress transient load swings also, may be required. An example of this is a rapidly varying load, such as a strip mill supplied from local generation as well as from a tie to a larger power system, in which it may be desirable to keep the load variations off the tie line as much as possible.

The characteristics and response of this kind of more active tie-line load controller have been studied in order to determine

1. The best tie-line controller and speed-governor characteristics (that is, the best controller characteristics for a given governor, and conversely the best governor characteristics for a given controller) to give the most effective tie-line power control.
2. The maximum effectiveness of the optimum controller in reducing the magnitude of tie-line power swings.

Basis of Study

The approximate torque equations for two interconnected speed-governed power systems were given in reference 2. Each system was represented as a single machine having an equivalent inertia, load-damping, and speed-governor characteristic, and the capacity of the tie line was specified by synchronizing torque and tie-line damping torque coefficients. In the present analysis the equation for one of the systems includes the additional effects of a tie-line power controller and (for a few cases) a speed droop-correction mechanism. The other system is considered to be infinitely large, that is, its electrical angle is fixed with respect to the standard frequency reference. The torque equations used and the definitions of the various system parameters are given in the appendix.

GOVERNOR MECHANISM

Speed-change and tie-line controller indications are applied to an idealized governor-control mechanism[2] consisting of a two-stage amplifier with time lags T_1 and T_2 and an over-all amplification factor inversely equal to the governor regulation. The prime-mover input torque change is equal, therefore, to the output of the second stage. Since the degree of stability of this idealized system tends to be somewhat greater than that of an actual governor having more than two time lags, practically all of the results have been obtained under the least stable condition of equal time lags.[3] In certain cases, the effects of droop correction have been simulated by allowing the regulation to decrease from the transient value, R, to the steady-state value, r, at a rate defined by the time constant T_3 of the droop compensation mechanism.[1]

TIE-LINE CONTROL

In the usual type of tie-line controller, the indications are transmitted intermittently at equal time intervals to the governing system through the synchronizing motor which is operated from a constant potential source. During each of the equal time intervals, usually of the order of two second period, the motor circuit is closed a length of time proportional to the deflection of the controller galvanometer or load sensitive device at the beginning of the interval. The average effect of these impulses is to change the synchronizing-motor position, and, consequently, the magnitude of the prime-mover input correction, at a rate proportional to the deviation in tie-line power from the scheduled value. Some of the results obtained from the differential-analyzer solution of the problem include the effects of this intermittent controller.

It is assumed that when using the corresponding continuously acting controller, the type principally considered in this analysis, the control circuit will be arranged to operate the synchronizing motor at a speed proportional to the tie-line power deviation. The rate of correction is inversely proportional to the controller regulation R_t'; or, R_t' is a measure of the time required to change the synchronizing-motor position an amount equivalent

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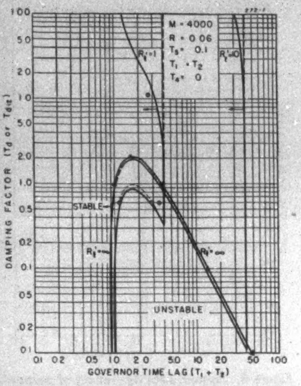


Figure 1. System stability—effects of droop compensation and of the floating type of tie-line control

—— T_d versus T_1+T_2, $T_{d12}=0$, $T_3=\infty$
– – – T_{d12} versus T_1+T_2, $T_d=0$, $T_3=\infty$
△ $r=0.03$, $T_3=5(T_1+T_2)$, $R_t'=\infty$
⊙ $r=0.03$, $T_3=5(T_1+T_2)$, $R_t'=1$

to a prime-mover torque change ΔP, when the controller is actuated by a fixed tie-line power deviation ΔP. The tie-line controller time constant T_4 is a measure of the time lag in the tie-line controller and synchronizing-motor response. Control circuits based on these principles are sometimes referred to as the "floating type" in that the correction is applied at a *rate* proportional to the change in the controlled variable, and the adjustment continues until the variable is restored to its normal value.

A "proportional type" of control is one that exerts a correcting force proportional to the change in the controlled quantity, as would be obtained, for example, with a tie-line controller that actuates a torque motor connected to the governor mechanism. The corresponding controller regulation, R_t, specifies that a tie-line power change ΔP results in a corrective indication equivalent to a prime-mover torque change $\Delta P/R_t$. A time lag T_5 is associated with this control system in the analysis.

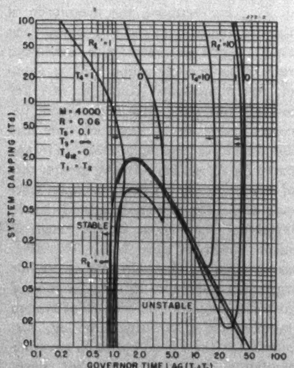


Figure 2. System stability—effect of time lag in the tie-line controller

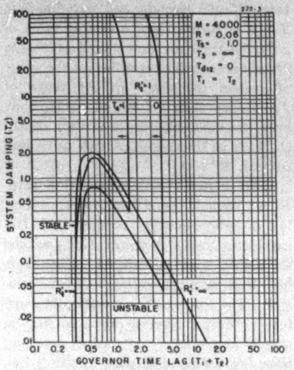


Figure 3. System stability—effects of floating type of tie-line control

Larger tie-line synchronizing power coefficient than that of Figures 1 and 2

LOAD-TORQUE CHANGE

The distribution of load increments between two systems is determined at the first instant by the electrical characteristics. In this study the entire load-torque change ΔT has been applied directly to the smaller system; a condition that would be realized if the impedance to the smaller system from the point of load application were negligibly small with respect to the impedance to the larger system. This assumption is justified, since most of the data have been taken with a tie line of small capacity with respect to even the smaller system.

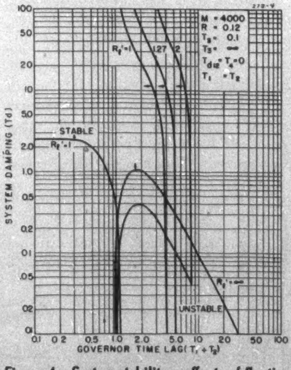


Figure 4. System stability—effects of floating type of tie-line control

Larger governor regulation than that of Figures 1 and 2

MAXIMUM RATES OF RESPONSE

In order to prevent large tie-line power swings the prime-mover power input to each system must vary with its load. The over-all power input to a group of generating units is limited, however, to a maximum rate depending upon the steam storage or hydraulic capacities of the various units. In some cases this rate may be of the order of ten per cent of the regulated capacity per minute.

Although these practical limitations have an important bearing upon the general problem of tie-line control, the ideal condition of an infinite prime-mover energy source has been assumed for the purpose of evaluating the desirable characteristics of the controller and governing systems.

Discussion of Results

One criterion for determining the optimum governor and tie-line controller characteristics is that the over-all system performance should be as stable as possible. Therefore, the effects of the various parameters upon system frequency stability have been studied. The direct calculations of stability limits are necessarily limited to those cases involving continuously acting (linear) tie-line control equipment.

Another desirable condition is that the tie-line power swings should be reduced to a minimum. The tie-line power changes for given load changes, calculated by means of the differential analyzer at the Moore School of Electrical Engineer-

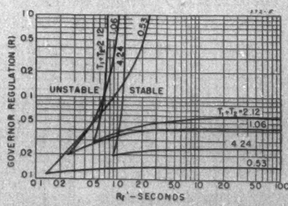


Figure 5. Permissible governor regulation and tie-line controller correcting time—effect of governor time lag

$M=4{,}000$ $T_5=0.1$ $R=0.06$
$T_d=2$ $T_1=T_2$ $T_4=0$
$T_{d12}=0$ $T_3=\infty$

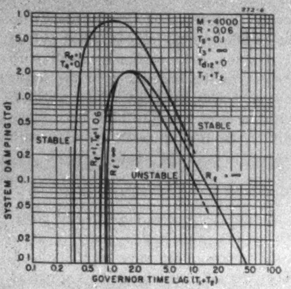


Figure 6. System stability—effect of proportional tie-line control

ing, University of Pennsylvania, are used as a basis for the conclusions regarding this phase of the problem.

STABILITY

Figure 1 shows the required load or tie-line damping as a function of total governor time lag, with various tie-line controllers, when the tie-line capacity is relatively small ($T_s=0.1$). The effect of the tie-line controllers may be seen by comparing the corresponding curves with the one obtained for a system having no tie-line control, $R_t'=\infty$. Also shown with this reference curve, which is similar to those of Figures 3 and 4 of reference 2, are the comparable points for a system having the same transient regulation, $R=0.06$, but a steady-state regulation of $r=0.03$ following the operation of a droop-correction mechanism with a time constant five times that of the governor control system. The stability limits are not appreciably changed provided the time lag T_s is sufficiently long.

1. *"Floating" Control.* The effect of a floating type of tie-line controller (Figure 1) with relatively slow correcting rate, $R_t'=10$ seconds, and zero time lag, $T_4=0$, is to introduce a critical value of governor time constant above which the system is unstable regardless of the value of system damping. In the range of T_1+T_2 less than the critical value, however, the unstable region is very nearly the same as that obtained with no tie-line control, $R_t'=\infty$.

With a much faster correcting rate, $R_t'=1$ second, the critical governor time lag is proportionally smaller, but the maximum value of required T_d within the stable region is reduced to less than half that required without tie-line control. The comparable points obtained for a governor having droop correction give a somewhat lower critical governor time lag. This difference appears, because R_t' has been taken as a measure of the equivalent correcting time when the controller indication is amplified only according to the transient governor regulation R, whereas the actual amplification of the adjustments increases as droop correction is applied.

The dashed curves of Figure 1 give the required tie-line damping, T_{d12}, with zero system damping, $T_d=0$, and are compared with the corresponding solid curves of required T_d with $T_{d12}=0$. With both types of damping present the curve of required total damping, T_d+T_{d12}, would lie between the two.

Introduction of time lag, T_4, in the tie-line control system (for example, in the synchronizing motor) results in a lower critical value of T_1+T_2, Figure 2, but otherwise the unstable region is not appreciably changed. Moreover, the percentage decrease of the critical governor time constant is more nearly a function of the ratio T_4/R_t' than of T_4 alone.

Figure 3 gives the required damping versus governor time lag when the tie-line capacity is much greater ($T_s=1.0$).

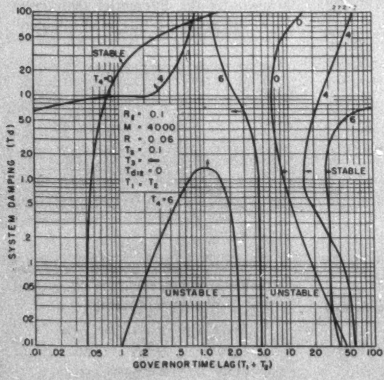


Figure 7. System stability—effect of proportional tie-line control

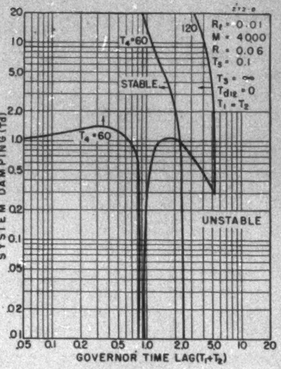


Figure 8. System stability—effect of proportional tie-line control

As shown in reference 2, and also by comparison of Figures 2 and 3, the maximum required T_d is unaffected, but the unstable region occurs in a range of lower values of T_1+T_2, when T_s is increased. The critical governor time lag introduced as a result of tie-line control is practically independent of T_s, and is approximately equal to $4R_t'$ for zero controller time lag and small system damping.

Results calculated with a larger regulation ($R=0.12$) and small tie-line capacity ($T_s=0.1$), Figure 4, are compared with those of Figures 1 and 2 for $R=0.06$. Without tie-line control ($R_t'=\infty$) the maximum required T_d is inversely proportional to the governor regulation, and the comparison of the unstable areas is similar to that of Figure 3, reference 2.

The other curves of Figure 4 indicate that the critical governor time lag is practically independent of R as well as of T_s. Furthermore, the magnitude of the maximum required T_d within the stable region decreases with increasing rate of tie-line correction. At a value of R_t' numerically equal to $MR/2\pi f$ seconds correcting time, the system becomes stable for all governor time lags less than its critical value as shown by the curve for $R_t'=1.27$. At any greater rate of correction ($R_t'=1$ is an example) however, and if the system damping is small, instability may occur in the range of small governor time lags. Therefore, the maximum allowable rate of tie-line correction (minimum R_t') is a function of governor regulation as well as of system damping and of governor time lag. Or, conversely, there is an optimum governor regulation smaller than one that causes instability, because of the above mentioned condition, and greater than one

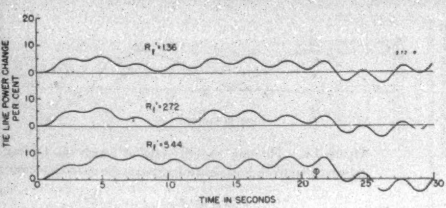


Figure 9. Floating control—magnitudes of tie-line power swings as affected by the rate of correction

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $T_4=1.06$
$T_{d12}=0$ $T_3=\infty$
$\Delta T=t/42.4$ for $0<t<21.2$
$\Delta T=0.5$ for $t<21.2$

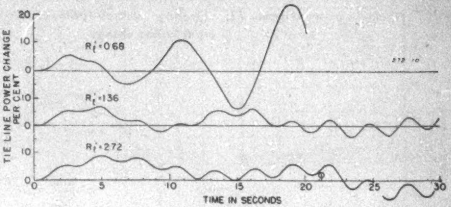


Figure 10. Floating control—magnitudes of tie-line power swings as affected by the rate of correction and by droop compensation

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $r=0.03125$
$T_{d12}=0$ $T_3=5.44$ $T_4=1.06$
$\Delta T=t/42.4$ for $0<t<21.2$
$\Delta T=0.5$ for $t<21.2$

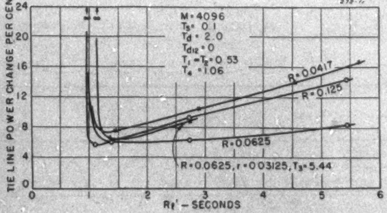
that causes instability, because of the large required T_d under normal conditions. One general rule for determining the maximum allowable rate of tie-line correction is, therefore, that R_l' should be greater than $MR/2\pi f$; that is, the possibility of an unstable region similar to the one for $R_l'=1$, Figure 4, should be avoided.

If the time lag of the governor is relatively large, instability is approached as the critical governor time lag approaches the actual time constant of the particular governing system. Under these conditions the minimum allowable R_l' is practically independent of the governor regulation, and with zero controller time lag it is approximately equal to four times the total governor time constant.

The above conclusions are substantiated by the curves of Figure 5 showing the permissible governor regulation versus correcting time of the tie-line controller for several different governor time constants and with $T_d=2$, $T_4=0$. The minimum allowable R_l' is in all cases a function of governor time constant, but for large values of governor time constant (T_1+T_2 equal to 2.12 and 4.24 seconds) it is practically independent of governor regulation.

Figure 11. Floating control—tie-line power deviation as affected by the governor regulation and by the rate of tie-line correction

$\Delta T=t/42.4$ for $0<t<21.2$
$\Delta T=0.5$ for $t<21.2$



2. *"Proportional" Control.* When the proportional type of tie-line controller is used, the steady-state change in tie-line power resulting from a load-torque change ΔT is (equation 1 of the appendix with $p=0$),

$$\Delta P=\frac{\Delta T}{1+\frac{1}{R_l}}=\frac{R_l\Delta T}{1+R_l}$$

The effects of a proportional controller with a very broad regulation, $R_l=1.0$, upon the stability limitations of the system having six per cent governor regulation and small tie-line capacity, $T_s=0.1$, are shown on Figure 6. With zero controller time lag, $T_3=0$, the region of instability and the maximum required T_d are both considerably greater than those calculated with no tie-line control. Satisfactory results are obtained, however, by adding a small amount of time lag as demonstrated by the results obtained with $T_3=1.06$ seconds. This value of controller regulation allows wide swings of tie-line power in that the load changes are equally divided between the two systems.

A controller regulation of $R_l=0.1$ is more nearly desirable in that the steady-state tie-line power change is limited to approximately nine per cent of the load change. With zero controller time lag, Figure 7, the required system damping is excessively large over the entire practical range of governor time lags. As the controller time lag is increased, the stable region in the range of small governor time lags is progressively changed until it acquires a form similar to the curves calculated with the R_l' type of controller. This effect is shown by the curves for T_3 equal to four and six seconds, Figure 7.

The response of a proportional controller with a time lag T_3 is given in the appendix as,

$$\frac{D}{R}=\frac{\Delta P}{pR_lT_3+R_l}$$

as compared to the response of the floating-type controller without time lag,

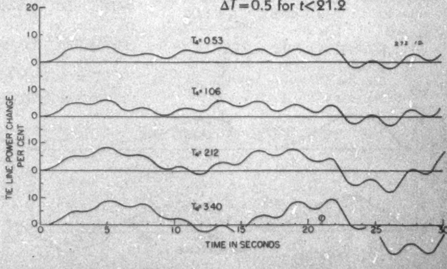
$$\frac{D}{R}=\frac{\Delta P}{pR_l'}$$

That is, for small R_l and large T_3 the product of the two is the governing factor as regards controller response, and this factor corresponds to the R_l' type of regulation.

Figure 8 is a good illustration in that the results for $R_l=0.01$ and T_3 equal to 60 and 120 seconds are similar to those of Figure 4 for the other type of control.

Figure 12. Effect of time lag in the floating type of tie-line control

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $R_l'=1.36$
$T_{d12}=0$ $T_3=\infty$
$\Delta T=t/42.4$ for $0<t<21.2$
$\Delta T=0.5$ for $t<21.2$



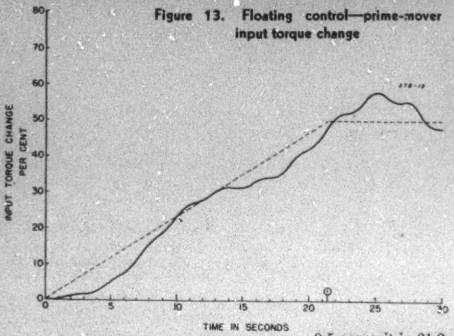


Figure 13. Floating control—prime-mover input torque change

——— Actual ----- Ideal

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $R_l'=1.36$
$T_{d12}=0$ $T_3=\infty$ $T_4=2.12$
$\Delta T=t/42.4$ for $0<t<21.2$
$\Delta T=0.5$ for $t<21.2$

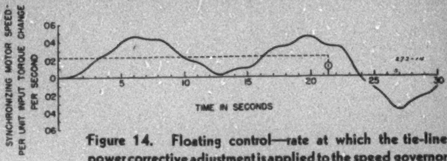


Figure 14. Floating control—rate at which the tie-line power corrective adjustment is applied to the speed governor

——— Actual ----- Ideal

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $R_l'=1.36$
$T_{d12}=0$ $T_3=\infty$ $T_4=2.12$
$\Delta T=t/42.4$ for $0<t<21.2$
$\Delta T=0.5$ for $t<21.2$

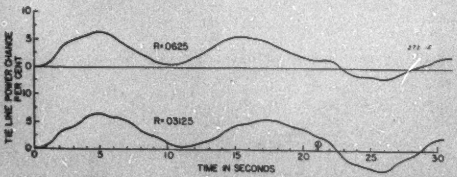
One disadvantage of proportional control is that it may require a more complicated mechanism for use with most governors. Furthermore, provision for obtaining a wide range of time lags may be required depending upon the required range of tie-line regulation.

TIE-LINE POWER SWINGS

The differential analyzer results were recorded in the form of curves of tie-line power, frequency, prime-mover torque, and controller response versus time during periods of large variable loads. A severe type of variable load, such as that furnished by a strip mill, is one in which a total variation as great as 50 per cent of the local generation capacity occurs in cycles of the order of one-minute periods. As an approximation to this condition, one set of runs was taken with a smooth load cycle in which ΔT increases at a uniform rate from zero to 0.5 per unit in 21.2 seconds, remains constant for another 10.6 seconds, decreases at a uniform rate from 0.5 to zero in another 21.2 seconds, and so on. Because the decreasing ΔT was applied from a transient condition, the magnitudes of the power swings during this period were occasionally greater than those of the increasing ΔT period. For the purposes of comparing the effects of various control characteristics on a common basis, therefore, the results presented in this paper give only the magnitudes of the swings up to the time at which the decreasing ΔT was applied. Another set of runs was taken with continuously increasing ΔT applied at a fixed rate.

Figure 15. Floating control—effect of governor regulation on the tie-line power deviation for a system having larger damping factor ($T_d=10$)

$M=4{,}096$ $T_s=0.1$ $R_l'=1.36$
$T_d=10$ $T_1=T_2=0.53$ $T_4=1.06$
$T_{d12}=0$ $T_3=\infty$
$\Delta T=t/42.4$ for $0<t<21.2$
$\Delta T=0.5$ for $t<21.2$



1. *Floating Control.* Governor- and tie-line-controller time lags each equal to 1.06 seconds, system damping of $T_d=2.0$, and small tie-line capacity ($T_s=0.1$), were assumed for one set of conditions. The curves of tie-line power versus time, Figure 9, show the relative effects of three different rates of controller response for a system having a governor regulation of $R=0.0625$. The load torque ΔT was increased uniformly from zero to 0.5 per unit in 21.2 seconds (point 1 on the time scale) and was then held constant for the remainder of the time interval shown. Of the three values of R_l' used, the smallest is most effective in limiting the power deviation, but this does not mean that R_l' should be decreased further. Although the system inertia and regulation are not exactly the same as those used in the stability calculations, the minimum allowable R_l' for stable operation may be estimated by considering the location of this operating point ($T_1+T_2=1.06$ and $T_d=2.0$) within the stable regions of Figures 1 and 2.

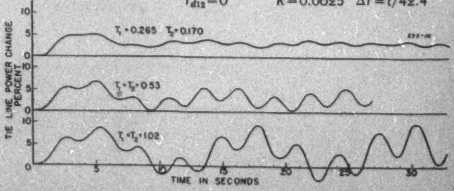
The tie-line power swings shown in Figure 10 are for a system with the same transient regulation ($R=0.0625$), but with a steady-state regulation of $r=0.03125$ obtained by means of a droop-correction mechanism having a time lag $T_3=5.44$ seconds. The run taken with $R_l'=0.68$ second demonstrates the instability resulting from too rapid tie-line power correction. As stated previously, R_l' is a measure of the equivalent correcting time when the amplification is inversely equal to the transient regulation R.

The maximum power swings of these runs, as well as those taken with governor regulations of 0.125 and 0.0417, are compared on Figure 11. The optimum controller response is nearly equal to that corresponding to the minimum allowable R_l', and under the assumed conditions the optimum governor regulation is of the order of six per cent. The tie-line power swings are not improved by adding droop correction.

Using the approximate optimum values indicated above, $R_l'=1.36$ seconds and $R=0.0625$, the controller time lag was varied. As shown by the curves of Figure 12, T_4 should be made as small as

Figure 16. Floating control—effect of governor time lag

$M=4{,}096$ $T_s=0.1$ $R_l'=1.36$
$T_d=2$ $T_3=\infty$ $T_4=1.06$
$T_{d12}=0$ $R=0.0625$ $\Delta T=t/42.4$



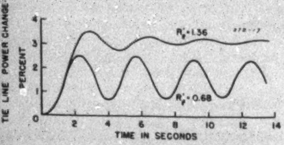


Figure 17. Floating control—effect of the rate of tie-line correction when there are no governor or controller time lags

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0$ $T_4=0$
$T_{d12}=0$ $T_3=\infty$ $\Delta T=t/42.4$

possible. This conclusion is compatible with the results of the stability calculations.

Figures 13 and 14, curves of prime-mover input torque change versus time and of synchronizing motor speed versus time, respectively, are for the same conditions as the power swing of Figure 12 with $T_4=2.12$. All of these quantities depend, of course, upon the rate of load-torque change. In actual practice a maximum correcting rate may be imposed because of boiler limitations, and so forth, previously discussed. The dashed curves of Figures 13 and 14 may be interpreted as showing the expected response of an idealized control system having no controller or governor time lags and zero correcting time.

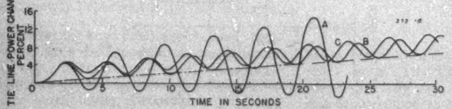
Natural frequency components of tie-line power swings are effectively reduced when the system damping is increased to $T_d=10$, Figure 15. The curve for $R=0.0625$ is compared with that of Figure 9 for $T_d=2$ and $R_l'=1.36$. It is observed that the maximum power swings are approximately equal. Although the larger system damping factor allows the use of a smaller governor regulation ($R=0.03125$), no appreciable over-all improvement is realized.

All of the remaining differential-analyzer runs were taken with a continually increasing load torque. For example, the effects of variations of the governor time constant are given on Figure 16 with ΔT applied at the rate of full torque change in 42.4 seconds. The system damping is $T_d=2$, and the governor regulation is $R=0.0625$. The rate of tie-line correction ($R_l'=1.36$) is the approximate optimum value (Figure 11) when the governor and controller time constants are each equal to 1.06 seconds. The curves are of interest in that an increase of governor time constant results in greater tie-line power swings. At a critical governor time lag of 2.04 seconds, the system becomes unstable.

Figure 18. Proportional control—tie-line power deviation as affected by the controller time lag

Curve A—$T_5=5.44$
Curve B—$T_5=8.15$
Curve C—$T_5=10.88$

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $R_l=0.10$
$T_{d12}=0$ $T_3=\infty$ $\Delta T=t/42.4$



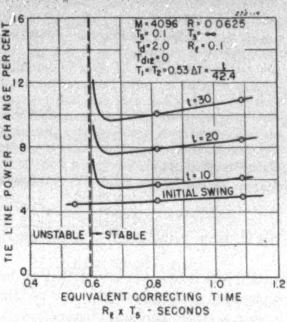


Figure 19. Summary of Figure 18 in terms of the equivalent controller correcting time ($R_l\times T_5$)

The optimum rate of tie-line correction depends upon the time constants of the control system. In the limiting case of zero controller and governor time lags, Figure 17, the maximum tie-line power change is less with $R_l'=0.68$ than with $R_l'=1.36$, although the natural frequency oscillations are not damped out as rapidly. The smaller R_l' is the one equal to $MR/2\pi f$ seconds correcting time (see section on stability).

2. *Proportional Control.* The curves of Figure 18 include the effects of a proportional tie-line controller having a regulation of $R_l=0.1$ and various amounts of time lag, T_5. The theoretical tie-line regulation is shown by the dashed line. Because of this drift of tie-line power the magnitudes of the initial swing and those at the end of 10, 20, and 30 seconds are summarized on Figure 19 as a function of the equivalent correcting time $R\times T_5$. The optimum correcting time is, as before, not much greater than the minimum value required to insure stable operation.

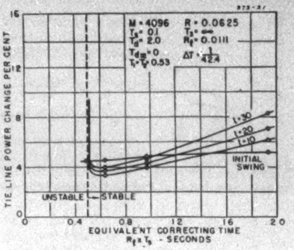


Figure 21. Summary of Figure 20 in terms of the equivalent controller correcting time ($R_l\times T_5$)

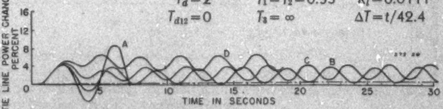
Figures 20 and 21 give similar information for a controller regulation of $R_l=0.0111$. The drift of tie-line power is very much less in this case, and with optimum controller time lag the power deviation at the end of 30 seconds is even less than that of the initial swing. The required controller time lag is much larger, however, being approximately inversely proportional to R_l. The system constants and the rate of load-torque change are the same as those used during the runs of Figure 16 for the floating type of control, and the results are, therefore, directly comparable. With the optimum proportional controller, Figure 21, the maximum tie-line power change is 4.6 per cent as compared to 6.8 per cent for the floating controller, Figure 16, with $R_l'=1.36$ and $T_4=1.06$. This difference would be even smaller if the optimum ($T_4=0$) floating-type controller had been used (see Figure 12 for effect of T_4).

3. *Intermittent Floating Control.* In applying the intermittent adjustments, the tie-line power deviation was read at intervals of 2.12 seconds. Following each reading a corrective indication was ap-

Figure 20. Proportional control—tie-line power deviation as affected by the controller time lag

Curve A—$T_5=$ 43 seconds
Curve B—$T_5=$ 58 seconds
Curve C—$T_5=$ 87 seconds
Curve D—$T_5=$174 seconds

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $R_l=0.0111$
$T_{d12}=0$ $T_3=\infty$ $\Delta T=t/42.4$



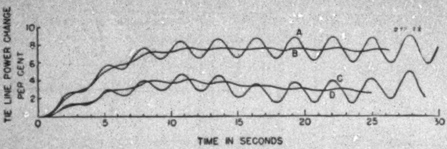


Figure 22. Intermittent control—tie-line power deviation as affected by system damping (T_d) and by the magnitude of load-torque change (ΔT)

Curve A—$T_d=2$, $\Delta T=t/84.8$
Curve B—$T_d=10$, $\Delta T=t/84.8$
Curve C—$T_d=2$, $\Delta T=t/169.6$
Curve D—$T_d=10$, $\Delta T=t/169.6$

$M=4{,}096$ $T_1=T_2=0.53$ $\Delta P_m=0.064$
$T_{d12}=0$ $T_3=\infty$ $k_1=1/42.4$
$T_s=0.1$ $T_4=1.06$ $R_l'(\text{eq.})=5.44$
$R=0.0625$

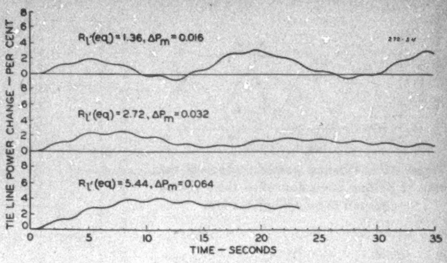


Figure 24. Intermittent control—effect of varying the equivalent controller correcting time by changing ΔP_m with k, constant

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=10$ $T_1=T_2=0.53$ $k_1=1/42.4$
$T_{d12}=0$ $T_3=\infty$ $T_4=1.06$
$\Delta T=t/169.6$

plied for a length of time proportional to the deviation ΔP at the beginning of the interval, and at a constant rate equivalent to k_1 per unit prime-mover torque change per second. The maximum duration of the impulse was limited to 1.06 seconds (half the period) for a power change equal to or greater than ΔP_m. For this controller there is an equivalent R_l' as given by (see appendix):

$$R_l'(\text{eq.})=\frac{2\Delta P_m}{k_1}$$

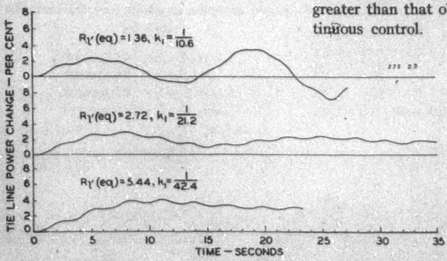
The tie-line power swings of Figure 22 were obtained with a controller having $T_4=1.06$, $\Delta P_m=0.064$ per unit, $k_1=1/42.4$, and $R_l'(\text{eq.})=5.44$ seconds. Curve A is compared with the curve $T_1=T_2=0.53$, Figure 16, for the continuous controller having the constants $R_l'=1.36$ and $T_4=1.06$. It is observed that the intermittent controller causes system instability, although its equivalent correcting rate is only one-fourth that of the continuous controller. For a system having a larger damping factor $T_d=10$, curve B of Figure 22, the results are satisfactory. Curves C and D give similar results with the load torque applied at one-half the rate. The magnitudes of the tie-line power swings are not proportional to the rate of load-torque change, because of the maximum correction rate imposed by this controller.

With $\Delta P_m=0.064$ and a large system damping, $T_d=10$, the effects of variations of the constant k_1 are shown on Figure 23. Likewise, Figure 24 gives the effects of variations of ΔP_m with $k_1=1/42.4$. The maximum power swings are summarized on Figure 25 as a function of the equivalent correcting time, R_l' (eq.). The optimum correcting time is of the order of 2.72 seconds under these conditions, that is, it is approximately twice that of the continuous controller. Furthermore, the effectiveness of a controller having the constants $\Delta P_m=0.032$ and $k_1=1/42.4$ is about the same as one with $\Delta P_m=0.064$ and $k_1=1/21.2$ since their equivalent correcting times are both equal to 2.72 seconds.

The power swings of Figures 23 and 24 for $R_l'(\text{eq.})=2.72$ are compared with that of Figure 15, $R=0.0625$, for the optimum continuous controller. After allowance has been made for the fact that the rates of applied load-torque change are different by a factor of four to one in the two cases, it appears that the tie-line power deviation obtained with the intermittent control is about 75 per cent greater than that obtained with the continuous control.

Figure 23. Intermittent control—effect of varying the equivalent controller correcting time by changing k, with ΔP_m constant

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=10$ $T_1=T_2=0.53$ $\Delta P_m=0.064$
$T_{d12}=0$ $T_3=\infty$ $T_4=1.06$
$\Delta T=t/169.6$

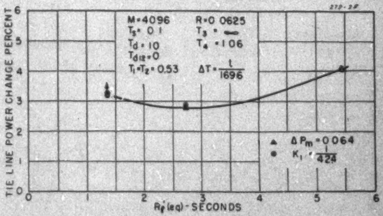


4. *Summary*. The control systems have been compared with respect to their effects upon the prime-mover governor response and tie-line power deviation, when the system is subjected to smooth variable loads. The recorded data also give a measure of the power deviation for the particular load cycles and system constants considered. It is to be expected, however, that the magnitude of the swings depends upon the period as well as the magnitude of the variable load, particularly if the period is very short. The shape of the load cycle may also be of considerable importance. Instantaneous load changes initiate natural-frequency power oscillations. As an example of the extreme condition, successive build up of tie-line power swings can occur if instantaneous load changes are applied at regular time intervals equal to the period of the natural-frequency oscillations.

Conclusions

1. (*a*) Introduction of tie-line power control of the floating type results in system instability, regardless of the value of system

Figure 25. Summary of Figures 23 and 24—tie-line power deviation as affected by the equivalent correcting time of the intermittent controller



damping, if the controller correcting time (R_l') is less than about one fourth of the governor time lag (Figures 1–4). The inherent instability ($T_1+T_2>4R_l'$) is not appreciably affected by variations of the tie-line synchronizing power coefficient T_s or the governor regulation R, but it does appear at an even smaller value of governor time lag if there is time lag (T_4) in the tie-line controller.

(*b*) Instability may also occur in the range of small governor time lags as a result of a large rate of tie-line correction (small R_l'). The appearance of this unstable region is a function of governor regulation, that is, it appears when R_l' is less than $MR/2\pi f$ seconds correcting time (Figure 4). In general, therefore, the minimum allowable R_l', as regards system stability, is a function of the governor time lag and is also a function of governor regulation if the governor time lag is small (Figure 5). Accordingly, there is an optimum governor regulation dependent upon the governor time lag, the system damping factor, and the rate of tie-line correction.

(*c*) With respect to the maximum effectiveness of the floating control in reducing the magnitude of tie-line power swings, the optimum correcting time is not much greater than the minimum allowable value required to insure stable operation (Figure 11). The controller time lag should be reduced to a minimum (Figure 12).

2. The use of a proportional tie-line controller having small regulation and zero time lag will cause instability unless the system damping is very large. Stability is obtained by introducing controller time lag T_4. With small regulation (R_l) and large time lag (T_4), the controller response is primarily a function of the factor $R_l\times T_4$ and is not much different from that of floating control since the above factor is similar to the R_l' type of regulation. On the basis of simplicity, the floating control may be preferable for use with most governors.

3. The impulses of an intermittent control having about two second period and a fast rate of response may result in system instability, if the operation is near the limit of stability without tie-line control. Otherwise, satisfactory results can be obtained, and the optimum equivalent correcting time [R_l'(eq.)] is of the order of two times the optimum R_l' of the continuous controller.

4. The optimum rates of response of tie-line controllers for maximum effectiveness are very much higher than those ordinarily used in power-system control.

5. The optimum continuously acting controllers limit the tie-line power deviation to approximately 15 per cent of the total load variation if the tie-line synchronizing power coefficient is small ($T_s=0.1$), and if the load variation occurs in smooth cycles of about one minute period. With an intermittent controller of the type considered, the tie-line power deviation may be limited to approximately 25 per cent of the load variation.

6. Previous considerations[1,2] have indicated that a turbine-governor regulation of the order of six per cent may be desirable. Although the optimum value depends to some extent upon the governor time lag, system damping, rate of tie-line correction, and so on, the present analysis has shown that six per cent regulation is satisfactory (Figures 11 and 15) when automatic tie-line control is also used. A smaller steady-state regulation is obtained without appreciable change in the degree of system stability by adding a droop-compensation mechanism having a relatively large time constant, but the tie-line power swings are not reduced in magnitude (Figures 1 and 11).

Appendix. Equations and Definitions of Terms

The approximate torque equations for two interconnected power systems were given in reference 2. If it is assumed that one system is infinitely large the equation for the smaller system is,

$$[Mp^2+(T_d+T_{d12})p+T_s]\delta+T_g=\Delta T \qquad (1)$$

where,

$M=4\pi fH$ is the effective inertia of the system and is equal to the time in electrical radians required to reach full rated speed from standstill with rated torque applied to the rotor.

T_d is the system damping factor equal to the slope of the net prime mover plus load torque versus speed curve with fixed prime-mover input valve.

T_{d12} is the tie-line damping-torque coefficient, or the change in torque per unit change in relative system speeds.

T_s is the synchronizing-torque coefficient, or the change in torque per electrical radian change in relative system angles.

T_g equals the change in prime-mover torque resulting from the movement of the input valves.

ΔT is that part of the load-torque change applied to this system.

All of the above quantities are in per unit on a kilovolt-ampere base equal to the system capacity.

δ is the effective system angle in radians with respect to the normal-frequency reference; or in this case with respect to the angle of the infinite system.

$p\delta=d\delta/dt$ is a measure of speed or frequency change.

t equals time in electrical radians; 377 radians = one second for a 60-cycle system.

The governor control mechanism has been represented as a two-stage amplifier having time lags T_1 and T_2.[2] The hydraulic-relay system of an actual governor might be considered as the first stage and the steam-storage capacity as the second. Speed response indications and tie-line control adjustments at the governor head

$$A=p\delta+D \qquad (2)$$

are transmitted to the first stage through a set of levers. The effective leverage of this connection may vary because of the action of a droop-compensation mechanism, and if the variation is at an exponential rate defined by the time constant, T_3, the motion transmitted to the first stage is

$$F=\frac{(T_3p+1)}{\left(T_3p+\frac{r}{R}\right)}A \qquad (3)$$

At the first instant ($p=\infty$) following a change at the governor head, the indications are directly impressed upon the governor control system, but in the steady-state condition following droop correction ($p=0$) they are amplified by a factor equal to the ratio (R/r) of the transient to steady-state governor regulations.

In passing through the governor control system this correcting force is modified by the time lags T_1 and T_2 and is amplified by a factor inversely equal to the transient regulation of the governor. Thus the outputs of the first and second stages are, respectively:

$$\left.\begin{aligned} Q&=\frac{1}{(T_1p+1)}F \\ T_g&=\frac{1}{R(T_2p+1)}Q \end{aligned}\right\} \qquad (4)$$

The complete expression for the change in prime-mover torque is, therefore,

$$T_g=\frac{1}{(T_2p+1)(T_1p+1)}\frac{(T_3p+1)}{\left(T_3p+\frac{r}{R}\right)}\times \left(\frac{p\delta}{R}+\frac{D}{R}\right) \qquad (5)$$

For a governor without a droop-correction mechanism, $T_3=\infty$ and R is the regulation. Otherwise, R and r are the transient and the steady-state governor regulations, respectively. T_3 is the time constant of the droop-compensation mechanism.

As used in equation 5, the terms $p\delta/R$ and D/R are a measure of the turbine speed response and tie-line control adjustments, respectively, expressed as the equivalent prime-mover torque changes that would result if there were no governor time lag ($T_1=T_2=0$) and if the adjustments were amplified only according to the transient governor regulation $R(T_3=\infty)$. The characteristics of the tie-line control system are interpreted in this paper by evaluating the equivalent response D/R in this manner. For instance, the response of one type of controller has been expressed as

$$\frac{D}{R}=\frac{\Delta P}{R_l'p(T_4p+1)} \qquad (6)$$

where

$\Delta P=(T_s+pT_{d12})\delta$ is the tie-line power change, and

T_4 is the time lag of the controller.

With zero controller time lag ($T_4=0$) the first derivative of this equation

$$p\left(\frac{D}{R}\right)=\frac{\Delta P}{R_l'} \qquad (7)$$

shows that the equivalent correction is applied at a rate inversely proportional to the constant R_l'.

A D-C Telemeter or D-C Selsyn for Aircraft

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Synopsis: This paper describes a d-c telemeter which is particularly adapted for transmitting to the instrument board indications of pressure, temperature, liquid level and of the position of the various controlling members of the airplane. The description comprises the principle, variations, characteristics, and application of this telemetering system. Although the applications described are primarily in connection with aircraft, the versatile nature of this device makes it suitable for many other applications.

The large number of indications which must be transmitted accurately to the instrument board of an airplane has created a demand for a telemetering system of small size and light weight. For many years the a-c Selsyn telemetering system has given excellent service in marine applications. Variations of this system are used successfully on aircraft. A search for a simpler and lighter system which will operate on direct current has led to the development of the d-c Selsyn telemetering system.

Principle

A SIMPLIFIED concept of the operation of the d-c Selsyn may be obtained by referring to Figure 1 which shows a coil wound on a toroidal iron core. This is a continuous single layer winding with two brushes supplying direct current at diametrically opposite points. It is evident that a magnetic pole will be set up in the core under each of the brushes, and that the magnetic field inside the core will be as shown. This field will follow the brushes as they are rotated. If a polarized permanent magnet rotor is placed inside the core, it will revolve so as to keep its direction of magnetization in line with the brushes.

The next step in the development is shown in Figure 2. The circular rheostat transmitter is tapped at intervals of 120 degrees and these taps are connected to similarly spaced taps on the receiver. This gives a result similar to that obtained in Figure 1 so that, within the accuracy limits to be defined later, the permanent magnet rotor will turn to a position having the same relation to the taps on the receiver winding as the brushes have to the corresponding taps on the transmitter winding. The windings shown are similar to delta connected three-phase windings. Connections similar to other polyphase windings may also be used and the windings may be concentrated in coils rather than being uniformly distributed on the core.

The use of a permanent magnet of high coercive force in combination with a copper damping shell fixed in the air gap between the rotor and the stator gives effective damping combined with high torque. Damping by a permanent magnet used in this manner involves no power loss as would be the case if an alternating field were used. The torque is a function of the product of the rotor and stator fluxes. Since the rotor field is of much higher magnetomotive force than the stator field, and since its excitation involves no electrical losses, it is evident that the inherent torque producing effectiveness is high. The high ratio of torque to weight obtainable with this system is one of its outstanding advantages. This makes it possible to use a comparatively light moving element which requires neither ball bearings nor jewel bearings, since the frictional errors are inappreciable with steel and bronze bearings. Since these bear-

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For another type of controller response

$$\frac{D}{R}=\frac{\Delta P}{R_l(T_5p+1)} \tag{8}$$

the equivalent correction is directly proportional to the tie-line power change, but it is delayed by the time lag T_5.

The effects of an intermittent controller are introduced as follows. The tie-line power change, ΔP, is read at regular time intervals. Within the period Δt following each reading a corrective indication is applied at a constant rate k_1 and for a length of time proportional to ΔP. This rate of correction is again expressed as an equivalent prime-mover torque change per unit of time. If the correction is applied through the synchronizing motor then k_1 is also a measure of the synchronizing-motor speed.

It is assumed that the impulse starts after an elapsed time t_1 dependent upon the value of ΔP at the beginning of the interval, and ends after a fixed time t_e. If the reading of tie-line power deviation is equal to or greater than ΔP_m, the full correction is applied, that is, t_1 is equal to some fixed time t_m. Then for any smaller power deviation ΔP the impulse is applied at the time t_1 given by

$$\frac{t_e-t_1}{t_e-t_m}=\frac{\Delta P}{\Delta P_m} \tag{9}$$

The correcting adjustment applied at the governor head is of the form

$$\frac{D}{R}=\frac{kt}{(T_4p+1)} \tag{10}$$

where (with t_0 = time at beginning of interval Δt, and $t_e < t_0+\Delta t$)

$k=0$ for $t_1>t>t_0$
$k=k_1$ for $t_e>t>t_1$
T_4 = time lag of the controller

At a fixed power deviation equal to ΔP_m, and neglecting the controller time lag ($T_4=0$), there is an average correcting rate over the entire interval equal to

$$p\left(\frac{D}{R}\right)=\frac{k_1(t_e-t_m)}{\Delta t} \tag{11}$$

This average rate can be defined by an equivalent R_l' similar to that of equation 7:

$$p\left(\frac{D}{R}\right)=\frac{\Delta P_m}{R_l'(\text{eq.})} \tag{12}$$

or from the above two equations:

$$R_l'(\text{eq.})=\frac{\Delta P_m\,\Delta t}{k_1(t_e-t_m)} \tag{13}$$

The effects of variations of the constants k_1 and ΔP_m were obtained from the differential analyzer solutions. The following time intervals were used:

$\Delta t = 2.12$ seconds
$t_m = 0.265$ second
$t_e = 1.325$ seconds

All time constants and the factors R_l' and $1/k_1$ are given in units of electrical radians in the above equations but in other parts of the paper they are given in seconds for a 60-cycle system. Otherwise the notation is consistent throughout.

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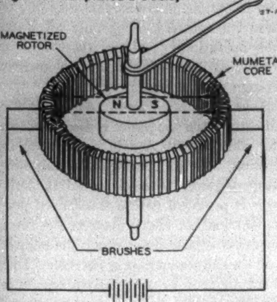
3. SUPPLEMENTARY CONTROL OF PRIME-MOVER SPEED GOVERNORS, S. B. Crary and J. B. McClure. AIEE TRANSACTIONS, volume 61, 1942, April section, pages 209–14.

Figure 1. Simplified d-c Selsyn


27-1
MAGNETIZED ROTOR
MUMETAL CORE
N
S
BRUSHES

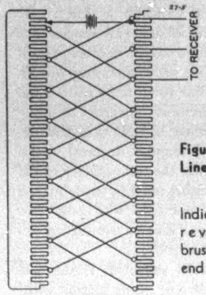

27-5
TO RECEIVER

Figure 5 (left). Linear transmitter for d-c Selsyn

Indicator makes three revolutions as brushes move from end to end of transmitter

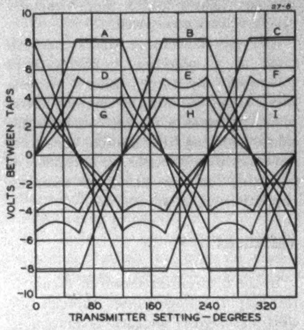


Figure 8. Curves showing voltage variations at taps on the transmitter with various loads connected to the transmitter and 12 volts applied

A—Taps 1–2, no load
B—Taps 2–3, no load
C—Taps 3–1, no load
D—Taps 1–2, one indicator
E—Taps 2–3, one indicator
F—Taps 3–1, one indicator
G—Taps 1–2, two indicators
H—Taps 2–3, two indicators
I—Taps 3–1, two indicators

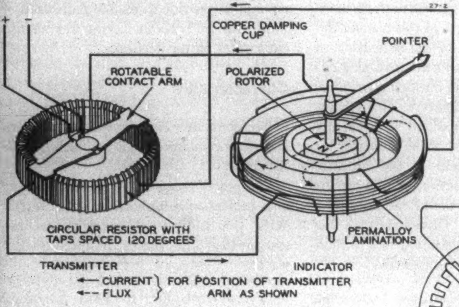

27-2
+
−
COPPER DAMPING CUP
POINTER
ROTATABLE CONTACT ARM
POLARIZED ROTOR
CIRCULAR RESISTOR WITH TAPS SPACED 120 DEGREES
PERMALLOY LAMINATIONS
TRANSMITTER
INDICATOR
← CURRENT
← FLUX
FOR POSITION OF TRANSMITTER ARM AS SHOWN

Figure 2 (left). D-c Selsyn telemetering system

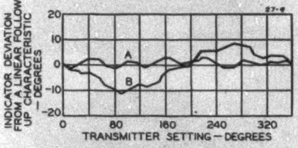


Figure 9. Deviation of d-c Selsyn from a linear follow-up characteristic

A—Instrument without power-failure indicator
B—Instrument with power-failure indicator
Either instrument will repeat its calibration curve within plus or minus 0.1 degree

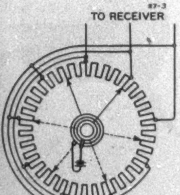

27-3
TO RECEIVER

Figure 3 (left). D-c Selsyn transmitter

Indicator makes three revolutions per revolution of transmitter

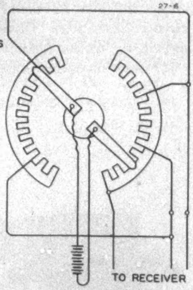
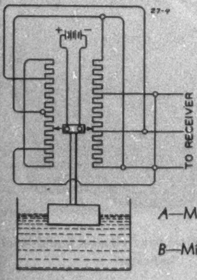

27-6
TO RECEIVER

Figure 6 (above). D-c Selsyn transmitter

Indicator makes one revolution as brushes traverse entire length of respective resistors

Figure 4 (below). Linear transmitter for d-c Selsyn

Indicator makes one revolution as brushes move from end to end of transmitter


27-4
+
−
TO RECEIVER

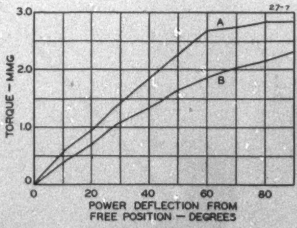


Figure 7 (above). Torque characteristics of d-c Selsyn

A—Maximum-torque condition with transmitter set with one brush on a tap
B—Minimum-torque condition with both brushes set 30 degrees from a tap

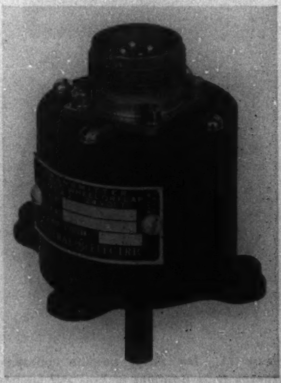


Figure 10. D-c Selsyn position transmitter

ings are lightly loaded they withstand severe vibration very well.

The use of sliding contacts has been cited as an objection to this type of telemeter. By using brushes of precious metal alloys operating at carefully determined pressures, a reliable contact is obtained with very little friction. Life tests have shown these brushes to be capable of operating for over 40,000,000 complete revolutions of the transmitter.

Variations

In the transmitter shown in Figure 2, the output voltage passes through 360 electrical degrees for each revolution so that the ratio of angular motions of the transmitter and indicator is unity. By making the number of evenly spaced taps equal to $3n$, where n is the number of indicator revolutions per transmitter revolution, it is possible to obtain a ratio equal to any whole number. Brushes of opposite polarity must be 180 electrical degrees $(360/(2n))$ angular degrees) apart. In order to utilize the full capacity of the winding, $2n$ brushes must be used, but the transmitter is operable with only two brushes. Figure 3 shows a transmitter designed for a ratio of 3.

The transmitter shown in Figure 4 is designed to transmit linear motion without converting it to circular motion by mechanical means. Motion of the brushes for the complete length of this transmitter gives one revolution of the indicator. A multi-revolution modification of this circuit is shown in Figure 5 which gives three revolutions of the indicator. This scheme can obviously be modified to give any number of revolutions of the indicator for a given linear motion of the brushes. Figure 6 is a modification of this circuit which will give fractional ratios with a circular transmitter. Obviously with this circuit, the motion is limited to less than one revolution of the transmitter.

Two or more indicators may be operated in parallel in connection with any of these transmitters. The only effect on their operation is to lower the torque in proportion to the reduction in current through each indicator.

Since there is no torque acting on the indicator when the power is disconnected, the pointer will remain unchanged in position. For applications where an indication of power failure is desired, the indicator is provided with a small fixed permanent magnet which attracts the rotor magnet to a position at which the pointer is off the calibrated part of the scale. Due to the high torque of the instrument, it is possible to use a fixed magnet strong enough to provide a reliable indication of power failure without having any significant effect on the accuracy.

Characteristics

Each indicator element weighs 25 grams and can be mounted in a $1^1/_4$-inch-diameter circle.

The power required by the transmitter and indicator combination is 1.8 watts. With two indicators connected to one transmitter, the power required is 2.2 watts.

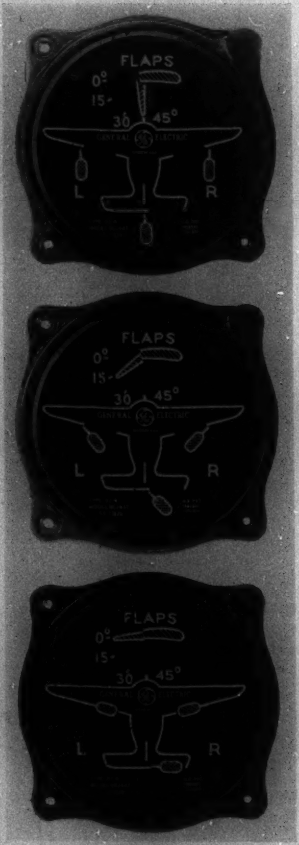


Figure 11. D-c Selsyn position indicators, showing three positions of flaps and landing gear

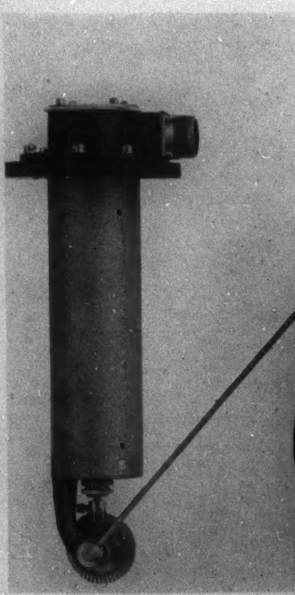


Figure 12. Typical liquid-level transmitter

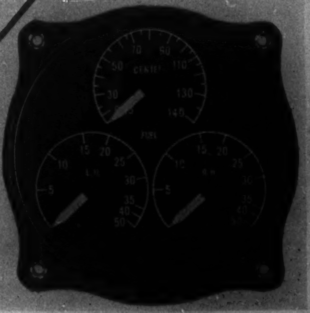


Figure 13. Typical three-element liquid-level indicator

Varying the voltage of the supply does not affect the indication provided no power failure indicator is used. With the power failure indicator a variation of ten per cent in the voltage will cause a maximum error of one degree.

The torque obtained is shown by the curves in Figure 7. The slope of the torque curve at zero is important since a steep slope at this point gives less frictional error. The torque curves show that the steepest part is at zero.

Temperature effect on indications is negligible because of the symmetry of the circuit.

The wave form of the voltage between taps in the transmitter is shown in Figure 8. At no load the form is triangular with the tops cut off flat. With load, the sides and tops of the form bend inward. The change in the form is of such nature that at any given point the ratio of voltage between one pair of taps to that between another pair of taps does not change with load so that an indicator connected to the transmitter will not change its calibration when another indicator is placed in parallel with it.

The peculiar shape of the voltage wave does prevent the indicator from following the transmitter linearly. This inherent deviation has been determined to have a maximum peak of 1.3 degrees. The peaks occur at six points above the axis, and six points below the axis. Figure 9 shows a typical distribution curve *A* which consists of the 1.3 degree deviation and additional deviation due to manufacturing tolerances. Curve *B* of the same figure shows how the distribution curve is changed by the power failure indicator.

Applications

One of the first applications of the d-c Selsyn was for the remote indication of the positions of the landing gear and wing flaps on an airplane. For this purpose four elements were mounted in a single case, one element being used for each of the three landing wheels and one for the flaps.

The remote indication of oil and fuel pressure is accomplished by connecting a bellows to the oil or fuel pressure line and utilizing the motion of the bellows to rotate the transmitter brushes.

The temperature transmitter has a bimetal helix which rotates the transmitter brushes as the temperature changes.

The manifold pressure transmitter has a partially evacuated sealed bellows in a pressure tight compartment and the manifold pressure is applied to the outside of the bellows. Motion of the bellows is transmitted mechanically through a flexible wall to the d-c Selsyn transmitter. Temperature compensation is obtained by having the right amount of air in the sealed bellows.

The differential fuel pressure transmitter is similar to the manifold pressure except the bellows is not evacuated. The fuel pressure is connected to the inside of the bellows while the surrounding chamber is connected to the manifold. The bellows deflection is then proportional to the differential pressure.

The liquid level measurement utilizes the motion of a float to actuate the transmitter brushes. Magnetic coupling is used between the transmitter outside the tank and the float mechanism inside the tank so as to provide a positive gasoline seal.

Conclusions

The d-c Selsyn has been shown to be particularly suited to applications requiring high accuracy, light weight, and rapid and stable indications. These qualities make it particularly suitable for aircraft use. The fact that it can be operated on any source of low-voltage direct current without auxiliary equipment is an advantage, not only for aircraft use but for other applications where special power sources are not available.

Design of Long-Scale Indicating Instruments

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I. Introduction

FROM the viewpoint of utility, the scale length of an indicating instrument bears somewhat the same relationship to its dimensions as does the output of a motor to its frame size. The end product of an instrument is quantitative information. The longer and more legible the scale, the more definite the resulting scale readings, through reduction of observational errors both in the initial calibration and in subsequent use. Thus, instrument designers have endeavored to produce maximum scale lengths within a given space, examples of which are 5-inch scales of 6-inch rectangular switchboard instruments, and the $3^1/_2$-inch scales of 4-inch rectangular instruments. In general, however, the movements of such instruments have been restricted to an angular deflection of about 90 degrees, with consequent limitation in scale lengths as compared to the scale lengths of nonelectrical instruments such as steam and pressure gauges. A study of these fundamentals leads the authors, who attacked the problem of designing a new line of switchboard instruments, to the conclusion that a definite contribution to measurement technique could best be made by designing such instruments for long-range indication, and the smallest practicable panel space.

A study of prior art[1-6] showed that the importance of scale length had been appreciated, although no complete and coordinated group of instruments had resulted. Among the long-scale instruments produced were wattmeters, voltmeters, and ammeters operating on the induction disk principle, also permanent-magnet moving-coil voltmeters and ammeters. The induction instruments, while perhaps satisfactory for restricted operating conditions, hardly met modern performance requirements as to frequency range, wave-form variation, and other conditions. There is very little published information on these instruments, and their manufacture appears to have been discontinued, at least in America. It was therefore concluded that no complete group of long-scale instruments, meeting modern installation and performance requirements, was available.

A distinguishing feature of switchboard instruments which cannot be overemphasized is the variety of measurements which must be accomplished in devices having uniform appearance and outline dimensions. These include the measurement of d-c volts, d-c amperes, a-c volts, a-c amperes, watts, vars, power factor, cycles, synchronism, and such nonelectrical quantities as temperature and speed. It is impractical to satisfactorily measure all these quantities with one design of instrument mechanism, a minimum of four being required as shown in Table A.

Table A

Group	Kind of Mechanism	Quantities Measured
A	Permanent-magnet moving-coil	D-c amperes D-c volts Temperature Speed
B	Repulsion – attraction with fixed and moving vanes	A-c amperes A-c volts
C	Iron-cored electrodynamic	A-c watts A-c vars A-c cycles
D	Electromagnet with rotating vane	Power factor Synchronism

In the following sections, the method of attack in designing long-scale elements for each of these groups will be outlined. A panel dimension of $4^1/_4$ inches square was required, and a scale length of 6.8 inches representing 240 angular degrees was selected. The problem in each case was to obtain (*a*) mechanical freedom of the movement and (*b*) satisfactory torque characteristics, both with respect to magnitude and gradient, over this scale angle. After determining each basic design, much analytical work was necessary to reduce it to practice, but, with one or two exceptions, design details will be omitted in the interest of brevity.

II. The Permanent-Magnet Moving-Coil Instrument for Measurement of D-C Potential and Current

The design of the permanent-magnet moving-coil instrument was definitely simplified by using Alnico as a magnet material. The high coercive force of this material adapted it to short lengths of large cross section, as illustrated in Figure 1. The sector-shaped magnet was cast integrally with a soft-iron pole face for uniform distribution of the field in the air gap, and the magnet was arranged immediately adjacent to the gap to minimize leakage fluxes. The moving coil was pivoted about one of its sides and thus only one of the coil sides was effective in production of torque. The use of two coil sides appeared to involve mechanical complications such as to be thoroughly impracticable. It was found that satisfactory results could be obtained by full utilization of the magnetic material as described above and by using a minimum of weight and inertia in the moving element. The millivoltmeter moving coil was designed for a power input of 0.34 milliwatt, since the instrument will be used with 50-millivolt shunts. This construction also has the advantages of shelf-shielding, mechanical simplicity, and reproducibility. Damping was obtained by adding a short-circuited winding to the moving coil, the wire size and number of turns being determined by the required damping constant of the instrument involved. A uniform scale distribution is obtained as shown in Figure 2. The characteristics are shown in Table II.

Since instruments for the measurement of the temperatures of electrical machinery are required for modern switch-

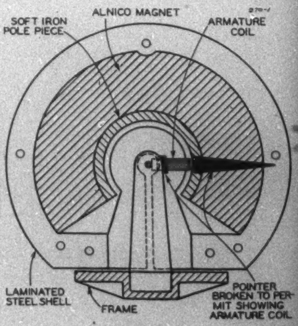

Figure 1. D-c ammeter and voltmeter mechanism

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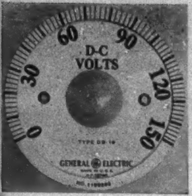

D-C
VOLTS
0
30
60
90
120
150
GENERAL ELECTRIC

Figure 2. D-c voltmeter-scale distribution

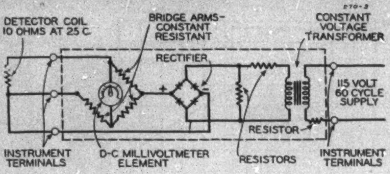

270-3
DETECTOR COIL 10 OHMS AT 25 C
BRIDGE ARMS-CONSTANT RESISTANT
CONSTANT VOLTAGE TRANSFORMER
RECTIFIER
115 VOLT 60 CYCLE SUPPLY
RESISTOR
INSTRUMENT TERMINALS
D-C MILLIVOLTMETER ELEMENT
RESISTORS
INSTRUMENT TERMINALS

Figure 3. Temperature-meter circuit

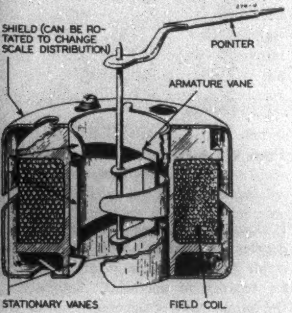

270-4
SHIELD (CAN BE ROTATED TO CHANGE SCALE DISTRIBUTION)
POINTER
ARMATURE VANE
STATIONARY VANES
FIELD COIL

Figure 4. A-c ammeter and voltmeter mechanism

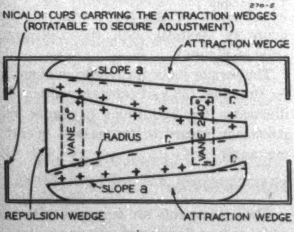
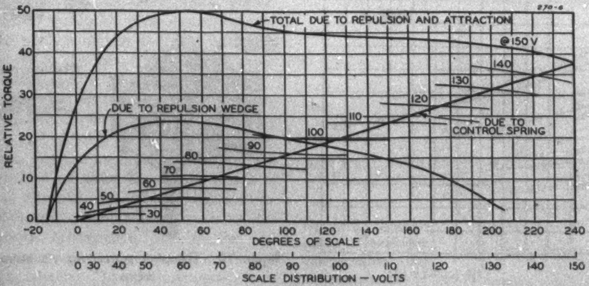

270-5
NICALOI CUPS CARRYING THE ATTRACTION WEDGES (ROTATABLE TO SECURE ADJUSTMENT)
ATTRACTION WEDGE
SLOPE a
VANE 0°
RADIUS
VANE 240°
SLOPE a
REPULSION WEDGE
ATTRACTION WEDGE

Figure 5 (above). Development of repulsion–attraction magnetic system

Figure 6 (below). Repulsion–attraction instrument torque characteristics along the scale



boards, such instruments have been included in this group. A circuit, illustrated in Figure 3, was thus designed to adapt the d-c milliammeter to temperature measurement using standard 10-ohm resistance temperature detectors. This circuit is essentially a Wheatstone bridge, energized by a constant potential of 6 volts direct current through a saturating transformer and rectifier designed for use on a standard 115-volt, 60-cycle circuit. The characteristics are given in Table II, and the scale distribution is shown in Figure 23.

III. The Repulsion–Attraction Instrument for Measurement of A-C Potential and Current

In their usual forms, the soft-iron torque-producing mechanisms are inherently limited to angular deflections of about 100 degrees of arc. Serious difficulties arise in matters of scale constriction and increased power consumption if the scales of these simple instruments are extended beyond their natural limits.

In the new element, these limitations are removed by utilizing dual forces of electromagnetic repulsion and attraction which act on a single movable vane as shown in Figures 4 and 5. The main repulsion mechanism is essentially similar to wedge-repulsion mechanisms now in use, in which a rapidly decreasing torque–displacement curve is obtained beyond the usual 0–90 degree scale range. The auxiliary irons exert a force of attraction which increases progressively as the repulsion force decreases. With suitable shapes and spacings for the soft-iron members, the two forces are well blended, resulting in a smooth open scale. In addition to the increase of scale angle, the element possesses a higher "torque-per-watt" effectiveness than any known soft-iron or even electrodynamic instrument mechanism, as illustrated in Table I.

The middle repulsion wedge is moulded integrally with the coil form. The attraction irons are mounted in magnetic contact with soft-iron cups which can be rotated manually through small angles. The rotatable cups serve as a means of instrument adjustment and also as a partial return path for the magnetic circuit. The cups are slotted radially to eliminate induced circulatory currents. Figure 4 is a sectional view of the assembly.

The rotatable cups (and attraction irons) permit of a rather wide range of control in scale distribution. The preferred scales are shown in Figure 9, where the voltmeter scale is expanded over the normal 110–120-volt interval. The ammeter scale is essentially uniform over the working range.

Table I. Sensitivity Comparisons of Various Instrument Mechanisms of Comparable Size

(Milliwatts Per Unit Torque for 90-Degree Deflection)

Repulsion–attraction	(soft-iron)	114
Wedge-repulsion	(soft-iron)	400
Inclined-coil	(soft-iron)	240
Electrodynamic	(air-coils)	216
Sector-coil repulsion	(soft-iron)	346

THE COIL

In proportion, an air coil of the greatest electromagnetic effectiveness (that is, maximum flux per watt input) will have a square cross section and a mean diameter equal to three times a side of the square as shown in the curves of Figure 7. The authors' curve of proportions is in agreement with Shawcross and Wells'.[2] The variance with Maxwell's classic proportions[3] of $d = 3.7c$ is due, no doubt, to neglect of higher order terms of the calculations. The two criteria are, however, high up on the curves so the variation is more of academic than practical interest.

The magnetic circuit of a repulsion–attraction mechanism of this kind permitted of little more than a rough mathematical treatment which served as a guide to methodical experimentation in determining the best shapes and spacings for the soft-iron members. From computations and from actual torque curves for

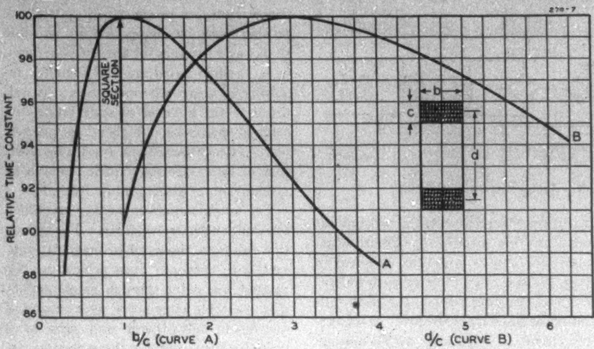
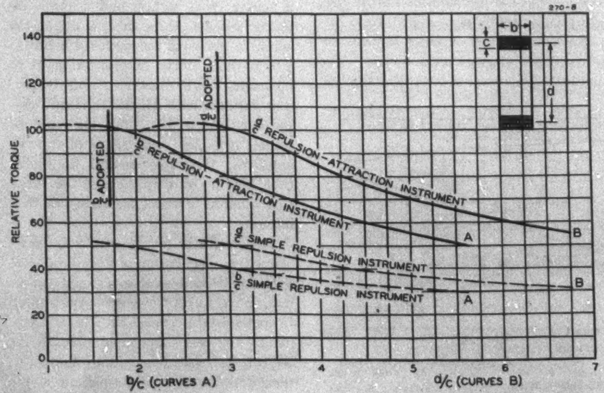


Figure 7. Field-coil proportions as a function of reactance/resistance

basic iron shapes, one could predict, quite closely, the ultimate results of iron shapes when used in combination. The best fixed repelling wedge to operate in conjunction with the ideal vane was one that best met the three criteria of steep initial torque-gradient, large angular deflection, and maximum torque. The initial steep gradient was especially desirable in ammeters for improved readability with low values of current. The wedge of Figure 5 met the requirements—the curved edges giving the steep gradient; the developed length, the long-scale angle; and the over-all dimensions, the maximum of torque.

The final and most important design problem was the selection of suitable attraction irons to extend the scale angle without increased power consumption. It was also necessary to avoid close spacings of adjacent magnetic parts, with their resulting manufacturing hazard. For adjustment purposes, the attraction irons permit of small rotational movements and are provided with means for securely locking them in the final position. The shape and slope of the irons result in a smoothly distributed scale, open to the end of pointer travel. The short curved lengths at the upscale ends of the attraction irons provide a greater degree of scale control. The curves (Figure 8) indicate that the coil system with irons, in its final form, has proportions differing very little from the requirements of ideal air coils.

Figure 8. Field-coil proportions with addition of iron members



Torque Characteristics

The torque values of the complete system over the scale, and for comparison, the torque of simple repulsion, is shown in Figure 6. The completely drawn, uppermost curve gives the torque at various angular scale positions with a constant application of full-scale voltage (150 volts). The negative angle of −15 degrees corresponds to the assigned unstable position of the pointer—selected for security of operation. Intersecting the torque curve of the control spring are sectional lengths of torque curves of intermediate voltages. From the intersectional points the actual scale distribution is obtained by projection. The projected scale at the lower edge of the figure is identical with the voltmeter scale of Figure 9.

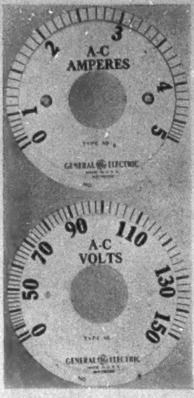


Figure 9. A-c ammeter- and voltmeter-scale distributions

The torque curve for repulsion alone is for the same 150-volt constant value. It illustrates the limitations of simple repulsion in regard to torque and operating angle.

Damping System

The instruments are equipped with an eddy-current damper of unique construction as shown in Figure 10. It consists of a series of ten uniformly spaced, bipolar magnets of Alnico steel, embedded in two

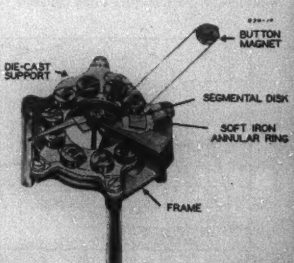


Figure 10. Damping system for a-c instruments

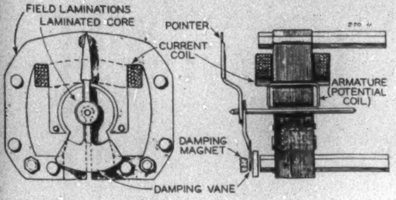


Figure 11. Wattmeter and varmeter mechanism

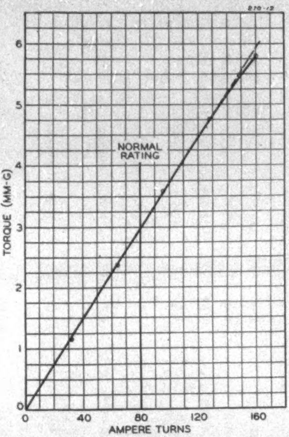


Figure 12 (left). Saturation curve of wattmeter field

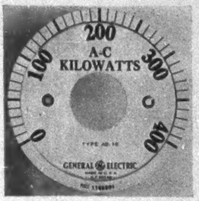


Figure 14. Wattmeter-scale distribution

die-cast half-ring supporting members. The two half-ring groups are assembled on the instrument in a circular arrangement directly over a small segmental aluminum disk, which is carried by the moving element. The magnetic circuit is completed through the disk to a soft-iron split annular-shaped ring mounted directly beneath the disk. The soft-iron ring serves also to shield the actuating element from the field projected by the damper magnets. This system provides an adequate and uniform damping torque throughout 360 degrees of disk movement. It is used on all a-c instruments.

Performance Characteristics

The characteristics of the a-c voltmeters and ammeters are shown in Table II. The efficient iron-clad mechanism has a high reactance-resistance ratio which is reflected in the instrument when constructed as a voltmeter. The effect of this reactance-resistance ratio on frequency influence was, however, overcome by capacitance compensation.

IV. The Iron-Cored Electrodynamic Instrument for the Measurement of Power

The long-scale instruments previously described deal with measurements of single quantities. Since there are important electrical measurements that involve the product or ratio of two electrical quantities, instruments must be provided for performing these mathematical operations. The measurement of the product of two electrical quantities suggested the use of the electrodynamic construction.

Since the conventional type of electrodynamometer instrument mechanism may be used for only a comparatively short-scale range, a modified construction was required for obtaining a scale length of 240 degrees. In order to obtain coordination of design and appearance, the general construction used in the permanent-magnet moving-coil instrument was adopted. In this instrument, however, the permanent magnet was replaced by a magnetic core shaped to permit insertion of a field winding and was designed for a uniform air gap between its pole face and the inner core, as illustrated in Figure 11. Since the magnetic field produced by the coil on the outer field structure is proportional to the current in that coil, the total torque produced by the instrument is proportional to the product of the ampere turns in the field and armature windings. The instrument is magnetically damped in the same manner as described for the iron-vane instruments.

The design of this mechanism presented several problems, particularly with respect to the magnetic circuit:

1. To produce a long scale, the use of magnetic material with attendant iron losses was required to obtain a long air gap.
2. The field circuit had to be designed for a linear flux-current relationship, requiring operations at densities well below the knee of the saturation curve at all possible operating currents.
3. The phase angle between the current and potential fluxes required adjustment to agree with the phase angle between the current and the voltage.
4. Losses in the instrument had to be kept low and voltage errors reduced to a minimum.

Several nickel-iron alloys were considered and tested for this application, and the best combination for low hysteresis and a high saturation density was obtained by the use of an alloy having 49 per cent nickel. The complete field structure, including the central core, was made of thin insulated laminations to reduce eddy currents to a minimum. Figure 12 shows the field density to be well below the saturation value for normal operating ranges.

The use of iron in the magnetic circuit naturally causes the current in the potential coil to lag slightly behind the applied voltage. This is more than canceled by the effect of the iron losses which cause the field flux to lag the line current by a still greater amount. Therefore, the

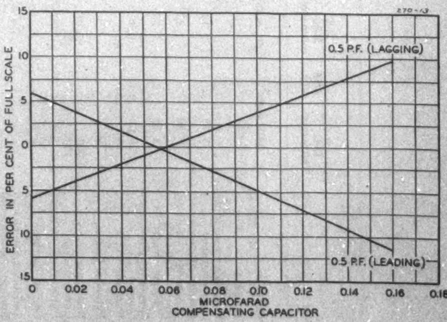


Figure 13. Determination of wattmeter compensation by capacitance

Table II. Long-Scale Instruments

Panel Space, 4¹/₄ by 4¹/₄ Inches Scale Length, 6.8 Inches

	D-C Voltmeter	ASA Stds.	D-C Milli-voltmeter	ASA Stds.	A-C Voltmeter	ASA Stds.	A-C Am-meter	ASA Stds.	Single-Phase Watt-meter	ASA Stds.	Polyphase Wattmeter	ASA Stds.	Polyphase Power-Factor Meter	ASA Stds.	Fre-quency Meter	ASA Stds.	Syn-chro-scope	ASA Stds.	A-C Tempera-ture Meter	ASA Stds.
Capacity	150. volts		50. millivolts		150. volts		5. amperes		115. volts 5. amperes 500. watts		115. volts 5. amperes 1,000. watts		115. volts 5. amperes 0-1-0 P.F.		115. volts 55-60-65 cycles		115. volts 60. cycles		115. volts 20-120 C	
Element																				
Full-scale torque (millimeter-grams)	3.76		2.14		3.80		3.80		3.47		5.4		37.9		4.0		16.7		1.52	
Weight of movement (grams)	1.49		1.41		2.62		2.62		2.30		3.9		8.2		4.2		8.2		1.65	
Ratio: full-scale torque/weight	2.52		1.52		1.45		1.45		1.51		1.38		4.62*		1.9*		2.04*		0.92*	
Response time (seconds)	1.8	2.0	1.6	2.0	1.6	2.5	1.6	2.5	1.6	2.5	1.3	2.5	1.3	2.5	1.8	2.5			1.5	
Damping-factor	10.	5.	50.	5.	17.8	5.	20.	5.	16.7	5.	19.0	5.	7.7	5.	10.	5.			49.	
Potential Circuit																				
Resistance (ohms)	15,000.		2.10		3,046.				5,800.		7,137.		3,725.						2,470.	
Ohms per volt	100.		42.		20.3				50.5		62.									
Burden (volt-amperes)	1.5		0.0012		7.4	9.			2.28	7.	1.85	7.	2.04**	10.	4.7	15.	1.92	(running)	3.76	
Current Circuit																				
Resistance (ohms)							0.050		0.023		0.023		0.103							
Burden (volt-amperes)							2.32	8.	2.55	5.	2.55	5.	3.6	8.			6.30	(incoming)		
Performance †																				
Full-load self-heating (ultimate)	Nil		Nil		−0.3		Nil		Nil		Nil		±0.4		±0.05				Nil	
Self-heating temperature rise (ultimate)	41.8 C	55 C	Nil	55 C	44 C	55 C	16 C	55 C	28 C	55 C	28 C	55 C	44 C	55 C	24 C	55 C	35 C	55 C	20 C	55 C
Ambient temperature coefficient (per cent per degree centigrade,	+0.005	0.035	−0.09	0.10	−0.012	0.025	+0.013	0.025	+0.02	0.025	+0.02	0.025	±0.03	0.05	+0.005	0.035			Footnote ●	
Permanent effect of 65 C (at 25 C)	±0.3		±0.3		±0.3		±0.2		±0.4		±0.4		±0.5		±0.083		Nil		±0.3	
Stray-field influence																				
(*a*) At 5 oersteds (greatest effect)	±0.28	1.0	±0.25	1.0	±2.	4.	±2.	4.	±0.1	4.	±0.1	4.	1.0	3.	±0.012	2.5			0.2°	
(*b*) At 5 oersteds (normal to axis)	±0.28	1.0	±0.25	1.0	±0.2	4.	±0.25	4.	±0.1	4.	±0.1	4.	1.0	3.	Nil	2.5			0.2°	
Voltage influence																				
(*a*) 90–130 volts															±.08					
(*b*) 110–120 volts (at scale ends)																			±0.8°	
(*c*) 105–130 volts (60–100 C)																			±0.8°	
(*d*) 105–130 volts (at scale ends)																			±1.4°	
(*e*) Over a ±10 per cent deviation									±0.2	0.5	±0.2	0.5	±0.5	0.5		0.25				

*240° Equivalent.

† Errors in per cent of full-scale value for voltmeters, millivoltmeters, ammeters, and wattmeters.
Errors in per cent of center-scale value for power-factor meter and frequency meter.
Errors in degrees centigrade for temperature meter.

** Per phase (three-phase winding).

● Temperature coefficient = ±0.064 C on scale per degree centigrade ambient rise at 20 and 120 C scale points. −0.015 C per degree centigrade at 80 C scale point.

Table II (continued). Long-Scale Instruments

Panel Space 4¼ by 4¼ Inches. Scale Length, 6.8 Inches

	D-C Voltmeter	ASA Stds.	D-C Millivoltmeter	ASA Stds.	A-C Voltmeter	ASA Stds.	A-C Ammeter	ASA Stds.	Single-Phase Wattmeter	ASA Stds.	Polyphase Wattmeter	ASA Stds.	Polyphase Power-Factor Meter	ASA Stds.	Frequency Meter	ASA Stds.	Synchroscope	ASA Stds.	A-C Temperature Meter	ASA Stds.
Performance†																				
Frequency influence																				
(a) Mean reversed direct current					+0.5		+2.													
(b) D-c hysteresis (max. effect)					±0.8		+2.													
(c) At 125 cycles					−0.7		−1.0		−0.3‡		−0.3‡									
(d) At 25 cycles					+0.7		+0.5		+0.5		+0.5								Footnote◐	
(e) Over a ±10 per cent deviation						0.5		0.5		0.5		0.5	±1.0⊗	0.5						
Permanent effect of 120 per cent load maintained for 6 hours			±0.3	0.35	0.3			0.35	±0.2	0.35	±0.1	0.35								
Permanent effect of momentary applications of 100 times full-scale current value							±0.4													
Power-factor influence																				
(a) 80 per cent lagging (rated volt-amperes)									±0.2		±0.2									
(b) 80 per cent leading (rated volt-amperes)									−0.3		±0.2									
(c) 50 per cent lagging (rated volt-amperes)									−1.0		−1.0									
(d) Zero power factor (rated volt-amperes)									±1.0	1.0	±1.0	1.0								
Current influence at 50 per cent power-factor																				
(a) 3⅓ amperes													0	1.0						
(b) 5 amperes													±0.9	1.0						
Lead resistance influence																				
Permissible resistance change for 1 C error (ohms)																				
(a) 20 C scale indication																			0.26	
(b) 80 C scale indication																			0.84	
(c) 120 C scale indication																			0.26	
Pull-in frequencies																				
(a) Ascending frequency																	58.0			
(b) Descending frequency																	62.0			
Drop-out frequencies																				
(a) Ascending frequency																	65.0			
(b) Descending frequencies																	56.0			

‡ At 80 cycles.

⊗ Over a range of 15 to 80 cycles.

◐ Error per cycle change in frequency: 1.0 C at 20 C scale point; 0.25 C at 80 C; 1.0 C at 120 C.

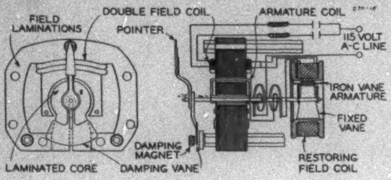


Figure 15 (above). Frequency - meter construction

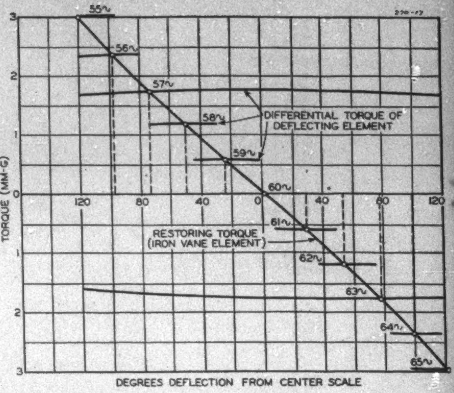


Figure 17 (right). Frequency - meter torque characteristics

potential-circuit current and the field flux are separated by a greater angle than the phase angle of the circuit, and, if uncompensated, would cause the instrument to read low on lagging power factors.

The instantaneous field flux then becomes proportional to

$$I_m \sin(\omega t-\theta-\alpha_2)$$

where α_2 is the angle by which the field flux lags the line current due to iron losses, and θ, the phase angle between current and voltage. The instantaneous armature current is proportional to

$$E_m \sin(\omega t-\alpha_1)$$

where α_1 is the phase angle by which the armature current lags the applied voltage. Without compensation, therefore, the torque would be—

$$T=KEI\cos(\theta-\alpha_1+\alpha_2)$$

Therefore, a capacitor is shunted across the armature and a portion of the series resistor, providing sufficient compensation to make the angle α_1 equal to angle α_2 giving correct indication at any power factor. Figure 13 shows curves taken at leading and lagging power factors indicating the effect of capacitance on compensation for power-factor errors. The curves show that the selection of a value of capacitance corresponding to the point where the two curves intersect, provides sufficient compensation to make the instrument operate with satisfactory accuracy over a wide range of power factors.

It has been mentioned that potential flux was reduced to a minimum to prevent voltage errors. With a high value of potential flux, the moving coil tends to deflect upon the application of potential alone, due to extraneous flux through the field iron and back across the air gap. While this potential creep can be calibrated out and become of little consequence, if maintained at low value, it would result in voltage errors if the extraneous torque so produced became an appreciable part of the total torque of the instrument. To minimize such errors, the armature turns were kept low, and high resistance was used to secure a minimum armature current consistent with high operating torque. The magnetic flux path is also broken by two gaps, the ratio of which determines the magnetic balance of the circuit and consequently the potential-circuit errors. They are, therefore, set to give the best overall performance.

Because of the uniform gap the scale distribution is nearly linear as shown by Figure 14.

The same type of mechanism is used for polyphase wattmeters, two elements being rigidly fastened together to the supporting base. In the polyphase instrument it is possible to still further increase the potential-circuit resistance in order to decrease voltage errors as the torque to weight ratio of the single-phase construction can be maintained without doubling the total instrument torque. Compensation for power-factor errors is made in the same manner as previously described using a double capacitor for this purpose.

When used for the measurement of vars (reactive volt-amperes), the instruments are provided with external phase-shifting auto transformers or are cross-phased to obtain the proper phase angle in the manner used on conventional switchboard instruments. Single-phase varmeters require an external impedance

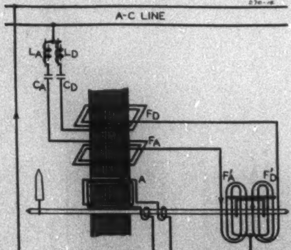


Figure 16. Frequency-meter circuit

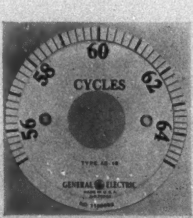


Figure 18. Frequency-meter scale distribution

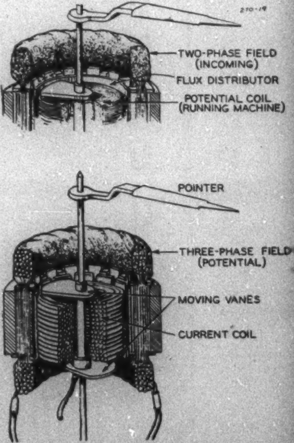

TWO-PHASE FIELD (INCOMING)
FLUX DISTRIBUTOR
POTENTIAL COIL (RUNNING MACHINE)
POINTER
THREE-PHASE FIELD (POTENTIAL)
MOVING VANES
CURRENT COIL

Figure 19. Power-factor meter and synchroscope mechanisms

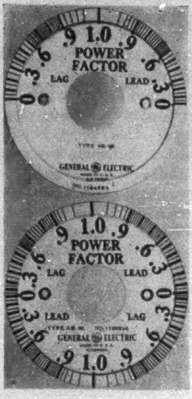


Figure 20. Power-factor-meter scale distribution

network to lag the current in the potential circuit by 90 degrees.

MEASUREMENT OF FREQUENCY

The iron-cored electrodynamic instrument mechanism was applied to the measurement of frequency by using a differential field coil,[4] each side of which was connected in a resonant circuit, one resonant at a frequency below the scale range, and the other, at a frequency above scale range. Since these two field coils are connected in opposition, an auxiliary element is added to provide restoring torque. The deflecting element has a uniform air gap and provides substantially uniform torque over its operating range. Thus the restoring element must also produce approximately uniform torque to obtain a scale as uniform as possible. Obviously with a 240-degree scale, a unidirectional and linear value of torque must be provided for 120 degrees each side of the center-scale position. Since it is evident that such a torque cannot be furnished by a simple dynamometer system, or any of the conventional iron-vane systems, a restoring torque has been provided by utilizing a long-range iron-vane repulsion mechanism similar to that used in the a-c ammeter and voltmeter. The construction is shown in Figure 15. It will be noted that the deflecting element is identical in construction with that used in the wattmeter, and that the restoring element is provided with a fixed iron vane which is wide at scale ends and narrow at center scale.

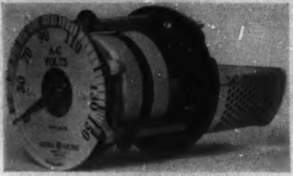


Figure 22. Interior view of a-c voltmeter

The connection diagram, Figure 16, shows the two resonant circuits and the differential connection of the deflecting and restoring fields, the resultant current passing through the armature coil. Variations of these connections are possible. For example, the connection of the restoring field coil with additive polarity, which somewhat changes the shape of the restoring torque curve, provides a scale which is expanded in the center and constricted toward the ends. With the subtractive polarity used, one circuit is resonant at about 34 cycles, while the other is resonant at about 97 cycles, both points being far outside the normal scale range of 55–65 cycles.

The curve in Figure 17 shows that approximately uniform restoring torque has been provided, its intersection with the differential torque curves at cardinal points indicating the angular displacement at these points.

It was necessary to give careful attention to several important points of construction. In the first place, the deflecting and restoring torque curves, extended beyond the scale ends, must be of such shape that the instrument will be stable at both ends of the scale with any condition of normal mechanical variation or changes in the resonant circuits. On a normal 60-cycle frequency meter with a scale of 55–65 cycles, stable operation is provided for about three times the normal frequency range above and below scale ends. The mechanical construction of the restoring field coil is likewise important in maintaining a scale which is essentially uniform and symmetrical. The spacing between the fixed and moving vanes is uniform, and the field winding is

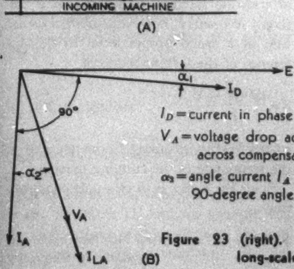


Figure 21 (left). Schematic connections and phase relations of synchroscope

A—Synchroscope connections

B—Phase relations

E = line voltage (incoming machine)

I_{LA} = current through reactance coil

I_A = current in stator coil A

α_1 = phase angle in circuit D

I_D = current in phase D (resistance circuit)

V_A = voltage drop across stator coil A (also across compensating resistance R_c)

α_2 = angle current I_A must lag V_A to produce 90-degree angle between I_A and I_D

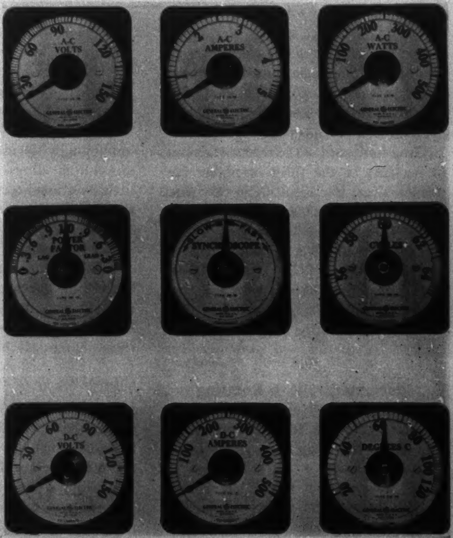


Figure 23 (right). Panel arrangement of long-scale instruments

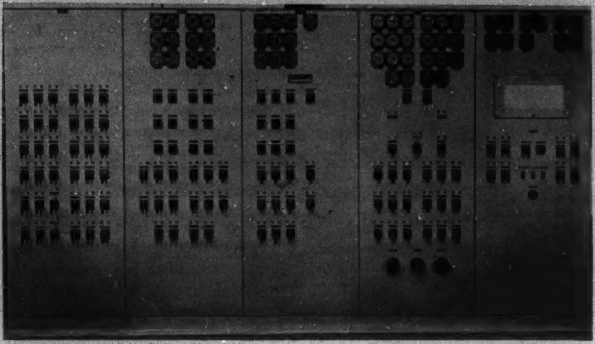


Figure 24. Typical installation of long-scale instruments

concentric with the stationary vane. Greater accuracy is obtained by winding the two coils at the same time, using two insulated wires in multiple.

In the frequency meter, the chief consideration was to make the instrument responsive to frequency changes without being influenced by any ordinary voltage fluctuations and with negligible disturbance from such operating variations as temperature and wave-form changes. It may be shown that the torque of both the deflecting and restoring elements is proportional to the square of the voltage and the deflection dependent on the ratio of currents in the two branches.

Angle of deflection $\alpha = \kappa \dfrac{1-r^2}{1+r^2-2r\cos\theta}$

(where r = the ratio of the currents in the two resonant branches and θ = the phase angle between them). The instrument indication is thus essentially independent of changes in the operating voltage.

The admittance of the resonant circuits to currents of the third or higher harmonics is very low, resulting in very small accuracy variations when used on distorted wave forms.

Figure 18 shows a standard 60-cycle scale with a range of 55–65 cycles. Other scale ranges are obtained by selecting impedance networks to provide the same current ratios over the required frequency range.

V. Instruments With Rotating Magnetic Field and Polarized Moving Vanes for Measurement of Power Factor

The power-factor meter has a field winding consisting of a motor-type stator with a laminated field structure and a distributed winding. This provides a rotating field when connected to a polyphase circuit. The armature is of the moving-vane type, the vanes of which are magnetically connected by a nickel-iron sleeve and polarized by an axially mounted stationary coil inside the stator. The construction is clearly shown in Figure 19. This system is not new, having been in use for a number of years in other forms,[5] but refinements in mechanical construction and improvements in operation make the instrument worthy of description.

When used to measure power factor, the field winding (stator) is connected through a high resistance to a polyphase line, while the vane is polarized by a stationary coil energized by the current in the circuit.

An analysis of this system will show that the instrument measures directly the phase angle between the current and the voltage and may be calibrated either in terms of power factor or phase angle. Hysteretic drag usually common to this construction has been reduced to a minimum by the use of carefully annealed nickel-iron alloys which produce very little rotational torque, and thus provide accurate readings over a wide range of current.

The movement is free to rotate throughout 360 degrees, and scales of either 180 degrees or 360 degrees can be provided, the latter where operation with reversed current is necessary. Figure 20 shows the scale ranges which may be obtained.

Indication of Synchronism

When designed for use as a synchroscope, the physical construction of the instrument is the same as the power-factor meter since the instrument actually measures the phase difference between the potentials of the bus and the incoming generator. The polarizing coil, however, is wound for potential instead of current and is connected directly across the line.

Since most synchroscopes are designed for single-phase operation, the stator circuit (connected to the incoming machine) was provided with a phase-splitting arrangement. This consists of an external impedor box containing a resistance and an adjustable reactance coil. The stator is of two-phase construction, one phase being connected in the resistance circuit and the other in series with the reactance. (See Figure 21.) The resistance of the A circuit and the inductance in the D circuit will produce a phase angle somewhat less than 90 degrees. The quadrature phase relation and a symmetrical rotating field is obtained by shunting a noninductive resistance R_c across the stator coil in the reactive circuit A. This produces a voltage drop V_A in phase with the lagging current I_{LA} through the reactor. The resistance R_c is of such a value that the inherent inductance in the stator causes the current through it (I_A) to lag the voltage drop V_A by an angle giving a 90-degree angle between I_A and I_D, the currents through the two stator branches. Final adjustment is made by a magnetic shunt on the reactance coil L_A.

Both "incoming" and "running" circuits were designed for low-power consumption, and the instrument, therefore, may be used for remote indication of synchronism. Uniform speed of rotation was obtained by the use of steel flux distributor rings as shown in Figure 19. These practically eliminated the slot effect of the stator and prevented spasmodic movement of the pointer at speeds approaching synchronism.

Since the armature vanes are polarized, the instrument becomes, in effect, a synchronous motor. If the polarizing coil were excited by direct current, synchronous operation would be obtained at a speed

$$S = \frac{120f}{P} \text{ (rpm)}$$

where f is the frequency and P the number of poles. The application of alternating current excitation of a different frequency, however, produces rotation at a speed proportional to the difference of these frequencies or

$$S = \frac{120(f_i - f_r)}{P}$$

where f_i = the incoming frequency and f_r the frequency of the running machine.

Since the stator is of two-pole construction

$S = 60(f_i - f_r)$ or 60 rpm for every cycle difference in frequency

Formulas for the Magnetic-Field Strength Near a Cylindrical Coil

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FELLOW AIEE

THE magnetic-field strength at any given point near a circular cylindrical coil or solenoid in a nonmagnetic medium requires various formulas for its determination, depending on the shape of the solenoid and the position of the point. Formulas are required for the axial component of the magnetic field, called H_x in this paper, and other formulas are needed for the radial component, H_r.

A number of the formulas were given in "Absolute Measurements in Electricity and Magnetism,"[1] by Andrew Gray, editions of 1893 and 1921. Others have been published in other articles, as indicated in the footnotes and references of this paper. A collection of formulas for this problem was published by the writer in "The Magnetic Field of a Circular Cylindrical Coil,"[2] *Philosophical Magazine*, volume 11, April 1931, page 948.

In this paper, an additional group of formulas is presented, suitable particularly for points close to the coil section and for short coils. A device is also given (see paragraphs following equation 3) by which formulas for the flux density in the end plane only of a solenoid are listed, and this greatly increases the range in which the flux density of solenoids under all conditions can be precisely and quickly calculated. It is shown in Figure 5 that the formulas listed in this paper cover the entire field of a solenoid.

All of the published series formulas for solenoids of which the writer is aware, have been rearranged in this form and are given in this paper. Thus this paper contains the complete equipment for computing the magnetic field of a solenoid of any shape and at any point, far or near, including points within the cross section of the winding. The effect of insulation space between conductors, however, is not considered, but the rectangular cross section of the solenoid is assumed to have uniform current distribution.

One application of the formulas and methods listed in this paper is in finding the mutual inductance of a solenoid and a comparatively small coil, particularly when the latter is irregular in its shape or position. In many cases the mutual inductance is equal to the magnetic field of the solenoid at the center of the small coil, multiplied by the projected area of the small coil, crossing the field. This is of use in problems of electromagnetic interference and shielding.

Logarithmic Formulas

The group of logarithmic formulas for points close to the coil section may be derived from a mutual-inductance formula for two circles which was published by T. H. Havelock[3] in 1908 and was extended by E. B. Rosa and F. W. Grover in equation 16, reference 4. It is given in equation 16A, reference 5.

If we let a circle of radius $y=a+c$ on the same axis as the circle of radius a, pass through P, Figure 1, then by differentiating equation 16A, reference 5, for the mutual inductance of the two circles, with respect to y or c, the change in mutual inductance for an increase in y is found. This is the change in flux passing through the circle of radius y when y is increased, caused by a continuous current in the circle of radius a. The increment in flux passes through the ring of width ∂y and circumference $2\pi y$.

If a certain amount of flux φ, for 1 centimeter perpendicular to the paper, passes through an area of width ∂y, at an angle θ to the normal to ∂y, as in Figure 2, then the flux density, or force on a unit magnetic pole is $\varphi/(\partial y \cos\theta)$. The horizontal component of the flux density is obtained by multiplying by $\cos\theta$, and is $\varphi/\partial y$.

Therefore, in the case of Figure 1, the axial component of flux density at radius y, or at P, is

$$\frac{1}{2\pi y}\frac{\partial M}{\partial y} \tag{1}$$

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The author acknowledges the work of the following students, done under his supervision in connection with theses written at the Massachusetts Institute of Technology:

Mason F. Miller, work on the derivation of equation 36 and other formulas, derivation of the first few terms of equation 8 by expansion of equation 13 in series, and comparison of calculated values of field strength with laboratory measurements of solenoids.

A. C. Louie, work on the derivation of equation 9, on the determination of boundaries of ranges in Figure 5 and comparisons with laboratory measurements.

A. L. Carpentier, work on properties of the formulas and determinations of boundaries of ranges in Figure 5.

Thus, the instrument will make one complete revolution for each cycle difference in frequency, the direction of rotation determining whether the incoming generator is slow or fast. Synchronism is indicated when the pointer reaches a vertical position.

VI. Conclusion

These instruments have been designed to meet existing American Standards Association standards of performance, as shown in Table II, and, in general, the characteristics are equivalent to those of 6-inch rectangular instruments. An accuracy rating of 1 per cent of the full-scale reading has been applied. The mechanical durability as evaluated by shipping, vibration, and impact tests is also fully equal to the standards established by present practice. The application of these instruments to a typical switchgear unit is shown in Figure 24. The group represents a potential panel-space saving of 42 per cent with an average weight saving of 16 per cent and an increased scale length of 33 per cent as compared to 6-inch rectangular instruments. It is concluded, therefore, that 90-degree scale angles need no longer be regarded as an inherent limitation of electrical indicating instruments, and instruments having long scales with satisfactory accuracy may be produced.

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2. On the Form of Coils to Give Maximum Self-Inductance for a Given Length and Thickness of Wire, R. E. Shawcross and R. I. Wells. *London Electrician*, volume 75, page 64.

3. A Treatise on Electricity and Magnetism, J. C. Maxwell. Volume 2, pages 345–6.

4. An Electrodynamic Moving Iron Mechanism for Electrical Measuring Instruments, T. A. Rich. *General Electric Review*, July 1935.

5. Electric Meters, C. M. Jansky. Second edition, pages 133–4 and 145–6.

6. Induction-Type Indicating Instruments, Paul MacGahan. AIEE Transactions, volume 31, 1912, pages 1565–77.

Putting $2a=d$ and $2y=D$, as in Figure 1, in order to make the numerical coefficients smaller, the following expression is obtained for the axial component of flux density at P due to a current of I amperes in a circular coil of N turns and of diameter d, assuming that the dimensions of the cross section of the coil are so small that they may be neglected:

$$H_{x(\text{circle})}=\frac{NI}{10}\times\frac{1}{2\pi y}\frac{\partial M}{\partial y}$$

$$=\frac{NI}{10}\frac{\sqrt{d}}{D\sqrt{D}}\left[\left\{\operatorname{logn}\frac{16Dd}{u^2}\right\}\times\left\{1-\frac{3}{4}\frac{u^2}{Dd}+\frac{45}{64}\frac{u^4}{D^2d^2}\cdots+\frac{c}{d}\left(\frac{3}{2}-\frac{15}{16}\frac{u^2}{Dd}\cdots\right)\right\}-\frac{2CD}{u^2}-\left(2-2\frac{u^2}{Dd}+\frac{123}{64}\frac{u^4}{D^2d^2}\cdots\right)+\frac{c}{d}\left(\frac{5}{2}-\frac{77}{32}\frac{u^2}{Dd}\cdots\right)\right]$$

lines per square centimeter (2)

where $u^2=x^2+c^2$ and where logn denotes natural logarithm.

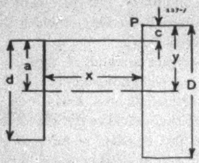

Figure 1. Magnetic field at P near a circle of radius a

Dimensions throughout this paper are in centimeters and electromagnetic centimeter-gram-second units are used, except where otherwise stated.

The current is assumed to be in such a direction around the circle that the flux density at the center of the circle is to the right. If the flux density given by equation 2 is a minus quantity, as it usually is when y is greater than a in Figure 1, that indicates that the direction of the flux at P is to the left. If y is less than a in Figure 1, the flux density is given by equation 2 but c has a negative numerical value.

Equation 2 and practically all the formulas of this paper, are infinite series, and they should be used for a given case only if the last term used in each series is almost negligibly small and is smaller than the preceding term. Otherwise, the succeeding terms which are neglected are probably of importance and the formula should not be used for such a case.

To obtain the axial component of flux density at P due to an infinitely thin solenoid, multiply expression 2 by $n\,dx/N$ and integrate from x_1 to x_2, taking these lengths to be positive values. See Figure 3. The quantity n is the number of turns per centimeter of axial length of the solenoid. The expression is 0 when x is 0, and so by putting $x_1=0$ and $x_2=x$ there is obtained the following formula 3 for the axial field at a point P in the end plane of a solenoid of length x, which is the only type of formula that needs to be listed for solenoids whose length is given:

$$H_{x(s)}=\frac{nI}{10}\left[\left\{\operatorname{logn}\frac{32yd}{x^2+c^2}\right\}\times\left\{\frac{x}{d}\left(1-\frac{3}{2}\frac{c}{d}+\frac{9}{4}\frac{c^2}{d^2}-\frac{55}{16}\frac{c^3}{d^3}+\frac{345}{64}\frac{c^4}{d^4}\cdots\right)-\frac{x^3}{d^3}\left(\frac{1}{4}-\frac{15}{16}\frac{c}{d}+\frac{75}{32}\frac{c^2}{d^2}\cdots\right)+\frac{9}{64}\frac{x^5}{d^5}\cdots\right\}-2\tan^{-1}\frac{x}{c}+\frac{x}{d}\left(\frac{1}{2}\frac{c}{d}-\frac{1}{2}\frac{c^2}{d^2}-\frac{3}{32}\frac{c^3}{d^3}+\frac{115}{64}\frac{c^4}{d^4}\cdots\right)+\frac{x^3}{d^3}\left(\frac{1}{2}-\frac{61}{32}\frac{c}{d}+\frac{151}{32}\frac{c^2}{d^2}\cdots\right)-\frac{21}{64}\frac{x^5}{d^5}\cdots\right]$$

lines per square centimeter (3)

The letter s in $H_{x(s)}$ denotes an infinitely thin solenoid or current sheet. This formula has been shortened somewhat by expressing D in terms of d, the mean diameter of the coil.

It is evident from Figure 3 that the magnetic flux density at P due to the actual solenoid of length (x_2-x_1) is equal to the difference between the field densities of two solenoids of lengths x_2 and x_1, respectively, of the same thickness, for both of which P is in the end plane. A physical meaning is thus given to the value of expression 3 for x_1 and x_2 separately, when integration is carried between these limits for the case shown in Figure 3.

It is, therefore, possible to calculate the value for x_1 by one formula, suitable for short coils, and that for x_2 by another formula if desired, suitable for long coils. The use of this device greatly increases the capability of calculating the flux density due to a solenoid under various conditions, with ease and precision.

A similar device for mutual inductance of solenoids was given in reference 5 (see paragraphs following equation 9 of that paper) and it produced a corresponding increase in the capacity to calculate the mutual inductance of coils of various shapes, in various positions.

A correction for the thickness, t, of the coil is desirable. Following Maxwell, Electricity and Magnetism,[6] paragraph 700, and noting that the differential of d is twice the differential of a radical distance

$$H_{(\text{coil})}=H_{(s)}+\frac{t^2}{3!}\frac{\partial^2 H_{(s)}}{\partial d^2}+\frac{t^4}{5!}\frac{\partial^4 H_{(s)}}{\partial d^4}\cdots \qquad (4)$$

where $H_{(s)}$ is the magnetic-field density, either axial or radial, at a given point P in the end plane due to the infinitely thin central solenoid whose diameter is d (see Figure 3).

Expressing equation 3 in terms of $D=2y$ instead of d, so that c is the only variable, and differentiating, we obtain the term in t^2 of equation 4.

$$H_{x(\text{coil})}=H_{x(s)}+\Delta H_x$$

where

$$\Delta H_x=\frac{nI}{10}\frac{t^2}{D^2}\left[\left\{\operatorname{logn}\frac{16D^2}{x^2+c^2}\right\}\times\left\{\frac{x}{D}\left(\frac{1}{12}+\frac{1}{16}\frac{c}{D}-\frac{7}{32}\frac{c^2}{D^2}\cdots\right)-\frac{9}{32}\frac{x^3}{D^3}\cdots\right\}-\frac{xD}{(x^2+c^2)}\left\{\frac{1}{3}+\frac{1}{2}\frac{c}{D}+\frac{5}{12}\frac{c^2}{D^2}+\frac{7}{48}\frac{c^3}{D^3}-\frac{21}{64}\frac{c^4}{D^4}\cdots-\frac{x^2}{D^2}\left(\frac{1}{12}+\frac{9}{16}\frac{c}{D}+\frac{45}{32}\frac{c^2}{D^2}\cdots\right)+\frac{3}{64}\frac{x^4}{D^4}\cdots\right\}-\frac{xcD^2}{(x^2+c^2)^2}\left\{\frac{2}{3}-\frac{2}{3}\frac{c}{D}-\frac{1}{3}\frac{c^2}{D^2}-\frac{1}{6}\frac{c^3}{D^3}-\frac{1}{24}\frac{c^4}{D^4}+\frac{7}{96}\frac{c^5}{D^5}\cdots+\frac{x^2c}{D^3}\left(\frac{1}{6}+\frac{3}{8}\frac{c}{D}+\frac{9}{16}\frac{c^2}{D^2}\cdots\right)-\frac{3}{32}\frac{x^4c}{D^5}\cdots\right\}-\frac{x}{D}\left(\frac{1}{2}+\frac{121}{96}\frac{c}{D}+\frac{175}{96}\frac{c^2}{D^2}\cdots\right)+\frac{33}{32}\frac{x^3}{D^3}\cdots\right] \qquad (5)$$

Equation 5 should be used only for thin solenoids where t is small and where the correction ΔH_x is a small percentage. For somewhat thicker coils, the following formula may be used, obtained by putting equation 3 in terms of $D=2y$ and then integrating over the cross section of the coil:

$$H_{x(\text{coil})}=\frac{nI}{10}\left[\left\{\frac{c}{t}\operatorname{logn}\frac{16D^2}{x^2+c^2}\right\}\times\left\{\frac{x}{D}\left(1+\frac{1}{4}\frac{c}{D}+\frac{1}{12}\frac{c^2}{D^2}+\frac{1}{64}\frac{c^3}{D^3}-\frac{7}{320}\frac{c^4}{D^4}\cdots\right)-\frac{x^3}{D^3}\left(\frac{1}{4}+\frac{9}{32}\frac{c}{D}+\frac{9}{32}\frac{c^2}{D^2}\cdots\right)+\frac{9}{64}\frac{x^5}{D^5}\cdots\right\}-\left\{\frac{x}{t}\operatorname{logn}\frac{x^2+c^2}{D^2}\right\}\left\{1+\frac{1}{4}\frac{x^2}{D^2}-\frac{19}{64}\frac{x^4}{D^4}\cdots\right\}+\left\{\tan^{-1}\frac{c}{x}\right\}\left\{\frac{2c}{t}-\frac{x}{t}\left(2\frac{x}{D}-\frac{2}{3}\frac{x^3}{D^3}+\frac{4}{5}\frac{x^5}{D^5}\cdots\right)\right\}-\frac{c}{t}\left\{\pi-\frac{x}{D}\left(2-\frac{1}{2}\frac{c}{D}-\frac{4}{9}\frac{c^2}{D^2}-\frac{59}{192}\frac{c^3}{D^3}-\frac{917}{4{,}800}\frac{c^4}{D^4}\cdots\right)-\frac{x^3}{D^3}\left(\frac{1}{6}-\frac{1}{6}\frac{c}{D}-\frac{103}{120}\frac{c^2}{D^2}\cdots\right)+\frac{151}{320}\frac{x^5}{D^5}\right\}\right]_{c=c_1}^{c_2}$$

lines per square centimeter (6)

If y is less than a_1, then c_1 and c_2 are negative. If y lies in value between a_1 and a_2, then c_1 is negative and c_2 is positive.

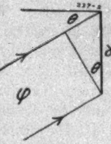

Figure 2. Flux passing through an area of width ∂y

Corresponding formulas are required for the radial component of magnetic field. Using equation 16, reference 4, for the mutual inductance of two circles, the radial field at P, Figure 1, due to a circle or coil of N turns of very small cross section, is

$$-\frac{NI}{10}\times\frac{1}{2\pi y}\frac{\partial M}{\partial x} \tag{7}$$

$$\begin{aligned}=H_{r(\text{circle})}=\frac{NI}{10D}\frac{\sqrt{d}}{D\sqrt{D}}\Bigg[&\frac{2xD}{u^2}+\\ &\frac{x}{d}\left(\frac{5}{2}-\frac{77}{32}\frac{u^2}{Dd}+\frac{141}{64}\frac{u^4}{D^2d^2}-\frac{17{,}165}{8{,}192}\frac{u^6}{D^3d^3}\cdots\right)\\ &-\left\{\operatorname{logn}\frac{16Dd}{u^2}\right\}\left\{\frac{3}{2}\frac{x}{d}\left(1-\frac{5}{8}\frac{u^2}{Dd}+\right.\right.\\ &\left.\left.\frac{35}{64}\frac{u^4}{D^2d^2}-\frac{525}{1{,}024}\frac{u^6}{D^3d^3}\cdots\right)\right\}\Bigg]\end{aligned}$$

$$\text{lines per square centimeter} \tag{8}$$

where $u^2=x^2+c^2$.

In equation 8, the dimension D, which is twice the radial distance to the point P, Figure 1, may be either larger or smaller than d, the diameter of the circle. Only even powers of the quantity c are involved.

The minus sign occurs in equation 7 since the mutual inductance of the two circles in Figure 1 decreases as x increases.

For the radial field at a point P in the end plane of an infinitely thin solenoid of diameter d, multiply equation 8 by ndx/N and integrate from 0 to x, where n is the number of turns per centimeter of axial length.

$$\begin{aligned}H_{r(s)}=\frac{nI\sqrt{d}}{10\sqrt{D}}\Bigg[&\left\{\operatorname{logn}\frac{16Dd}{c^2}\right\}\times\\ &\left\{1+\frac{3}{4}\frac{c^2}{Dd}-\frac{15}{64}\frac{c^4}{D^2d^2}+\frac{35}{256}\frac{c^6}{D^3d^3}-\right.\\ &\left.\frac{1{,}575}{128^2}\frac{c^8}{D^4d^4}\cdots\right\}-\left\{\operatorname{logn}\frac{16Dd}{u^2}\right\}\times\\ &\left\{1+\frac{3}{4}\frac{u^2}{Dd}-\frac{15}{64}\frac{u^4}{D^2d^2}+\frac{35}{256}\frac{u^6}{D^3d^3}-\right.\\ &\left.\frac{1{,}575}{128^2}\frac{u^8}{D^4d^4}\cdots\right\}+\frac{u^2-c^2}{2Dd}-\frac{31}{64}\frac{u^4-c^4}{D^2d^2}+\\ &\frac{247}{768}\frac{u^6-c^6}{D^3d^3}-\frac{7{,}795}{128\times256}\frac{u^8-c^8}{D^4d^4}\cdots\Bigg]\end{aligned} \tag{9}$$

where $u^2=x^2+c^2$. Here also, $D=2y$ may be greater or less than d.

Note that the radial field close to the end of an infinitely thin solenoid becomes infinitely great, because c approaches 0 and logn c is involved.

By equation 4

$$H_{r(\text{coil})}=H_{r(s)}+\Delta H_r$$

where

$$\begin{aligned}\Delta H_r=\frac{nI}{10}\Bigg[&\frac{t^2}{D^2}\left\{\operatorname{logn}\frac{x^2+c^2}{c^2}\right\}\times\\ &\left\{\frac{1}{12}+\frac{1}{4}\frac{c}{D}+\frac{17}{32}\frac{c^2}{D^2}+\frac{95}{96}\frac{c^3}{D^3}\cdots\right\}-\\ &\frac{t^2}{D^2}\left\{\operatorname{logn}\frac{16D^2}{x^2+c^2}\right\}\left\{\frac{x^2}{D^2}\left(\frac{7}{32}+\frac{15}{32}\frac{c}{D}\cdots\right)\right\}+\\ &\frac{t^2}{x^2+c^2}\left\{\frac{1}{3}-\frac{c}{D}+\frac{5}{12}\frac{c^2}{D^2}+\frac{7}{12}\frac{c^3}{D^3}+\frac{51}{64}\frac{c^4}{D^4}+\right.\\ &\frac{247}{96}\frac{c^5}{D^5}\cdots+\frac{x^2}{D^2}\left(\frac{1}{4}+\frac{3}{4}\frac{c}{D}+\frac{35}{32}\frac{c^2}{D^2}+\right.\\ &\left.\left.\frac{35}{32}\frac{c^3}{D^3}\cdots\right)-\frac{x^4}{D^4}\left(\frac{5}{64}+\frac{45}{64}\frac{c}{D}\cdots\right)\right\}-\\ &\frac{t^2c^2}{(x^2+c^2)^2}\left\{\frac{2}{3}-\frac{2}{3}\frac{c}{D}+\frac{1}{6}\frac{c^2}{D^2}+\frac{1}{6}\frac{c^3}{D^3}+\right.\\ &\frac{17}{96}\frac{c^4}{D^4}+\frac{19}{96}\frac{c^5}{D^5}\cdots+\frac{x^2}{D^2}\left(\frac{1}{2}+\frac{1}{2}\frac{c}{D}+\right.\\ &\left.\left.\frac{7}{16}\frac{c^2}{D^2}+\frac{5}{16}\frac{c^3}{D^3}\cdots\right)-\frac{x^4}{D^4}\left(\frac{5}{32}+\frac{15}{32}\frac{c}{D}\cdots\right)\right\}+\\ &\frac{t^2}{c^2}\left\{\frac{1}{3}+\frac{1}{3}\frac{c}{D}-\frac{1}{4}\frac{c^2}{D^2}-\frac{5}{12}\frac{c^3}{D^3}-\frac{119}{192}\frac{c^4}{D^4}-\right.\\ &\left.\frac{57}{64}\frac{c^5}{D^5}\cdots+\frac{x^2}{D^2}\left(\frac{89}{96}\frac{c^2}{D^2}+\frac{101}{32}\frac{c^3}{D^3}\cdots\right)\right\}\Bigg]\end{aligned} \tag{10}$$

Since equation 10 is applicable only to thin solenoids with a small value of t^2/c^2 because terms in t^4 and higher powers of t have been omitted, the following formula, obtained by integration of equation 9 after it was expressed in terms of D, is given for use with thicker coils:

$$\begin{aligned}H_{r(\text{coil})}=\frac{nI}{10}\Bigg[&\frac{x^2}{Dt}\left\{\operatorname{logn}\frac{16D^2}{x^2+c^2}\right\}\times\\ &\left\{\frac{1}{2}-\frac{3}{4}\frac{c}{D}-\frac{3}{8}\frac{c^2}{D^2}-\frac{7}{32}\frac{c^3}{D^3}-\frac{15}{128}\frac{c^4}{D^4}\cdots-\right.\\ &\left.\frac{x^2}{D^2}\left(\frac{5}{16}-\frac{15}{64}\frac{c}{D}-\frac{45}{128}\frac{c^2}{D^2}\cdots\right)+\frac{161}{384}\frac{x^4}{D^4}\cdots\right\}+\\ &\frac{c}{t}\left\{\operatorname{logn}\frac{x^2+c^2}{c^2}\right\}\left\{1-\frac{1}{2}\frac{c}{D}+\frac{1}{12}\frac{c^2}{D^2}+\frac{1}{16}\frac{c^3}{D^3}+\right.\\ &\left.\frac{17}{320}\frac{c^4}{D^4}+\frac{19}{384}\frac{c^5}{D^5}\cdots\right\}+\frac{2x}{t}\left(\tan^{-1}\frac{c}{x}\right)\times\\ &\left(1+\frac{2}{3}\frac{x^2}{D^2}-\frac{2}{5}\frac{x^4}{D^4}\cdots\right)-\frac{c}{t}\frac{x^2}{D^2}\left(\frac{5}{6}-\frac{11}{16}\frac{c}{D}-\right.\\ &\left.\frac{49}{60}\frac{c^2}{D^2}-\frac{145}{192}\frac{c^3}{D^3}\cdots\right)+\frac{c}{t}\frac{x^4}{D^4}\times\\ &\left(\frac{101}{320}-\frac{13}{24}\frac{c}{D}\cdots\right)\Bigg]_{c=c_1}^{c_2}\end{aligned}$$

$$\text{lines per square centimeter} \tag{11}$$

If y is less than a_1, then c_1 and c_2 are negative. If y lies in value between a_1 and a_2, then c_1 is negative and c_2 is positive.

Note that if the current is assumed to flow in a mathematically exact rectangular cross section, the radial field is not infinite at the corners or elsewhere. The limit of c logn c is 0 when c approaches 0. See reference 9, numbers 72 and 605.

Elliptic Integral Formulas for Circles

The following general formulas for the flux density in any position whatever, relative to a circle which carries a current, have been published by Alexander Russell[7] and give the same results as equations 2 and 8:

$$H_{x(\text{circle})}=\frac{2NI}{10r_1}\left\{\frac{2a(a-y)}{r_2^2}E+(K-E)\right\} \tag{12}$$

$$H_{r(\text{circle})}=\frac{2NI}{10r_1}\left\{\frac{2ax}{r_2^2}E-\frac{x}{y}(K-E)\right\} \tag{13}$$

where the dimensions are as in Figure 1 and where

$$r_1^2=(a+y)^2+x^2 \tag{14}$$

$$r_2^2=(a-y)^2+x^2=c^2+x^2 \tag{15}$$

K and E are complete elliptic integrals of the first and second kinds of modulus k, where

$$k^2=1-r_2^2/r_1^2 \tag{16}$$

Values of K and E may be taken from tables, as for instance, reference 8, pages 199 and 204.

It is seen from Figure 1 that r_2 is the distance from P to the nearest part of the circumference of the circle carrying current. If this distance is very small, k^2 approaches 1 and the value of the elliptic integral K approaches infinity. When this occurs, the values in any table become far apart so that interpolated values cannot be obtained with precision and it becomes better to use a series involving logarithms than to use a table of values. That, however, is equivalent to using formulas 2 and 8, which involve logarithms. It is always possible to find the precise value of the logarithm of any given number, however large or small, one way being to find first the logarithm to base 10.

Since equations 2 and 8 are power series in r_2^2/ay, they have greater precision, the closer the point P approaches to the circumference of the circle and the greater becomes the difficulty of obtaining the value of K from a table.

Zonal Harmonic Formulas for Circles

Other formulas will now be listed. The formulas for solenoids are put in the more useful and usually more concise form giving the flux density at a point in the end plane of the solenoid.

For points near the center of the circle,

$$H_{x(\text{circle})}=\frac{2\pi NI}{10a}\left[1-\frac{3}{2}\frac{r^2}{a^2}P_2\left(\frac{x}{r}\right)+\frac{3\times 5}{2\times 4}\frac{r^4}{a^4}P_4\left(\frac{x}{r}\right)-\ldots\right] \tag{17}$$

where

$$r^2=x^2+y^2 \tag{18}$$

$P_2(x/r)$, $P_4(x/r)$ and so on, are surface zonal harmonics which may be defined by

$$P_n(\mu)=\frac{1}{2^n n!}\frac{\partial^n}{\partial\mu^n}(\mu^2-1)^n$$

Values are tabulated in reference 8, page 188, reference 10, and elsewhere, or they may be calculated from series (see reference 9, page 169 and equation 46 of this paper).

$$H_{r(\text{circle})}=\frac{\pi NI}{10}\frac{yr}{a^3}\left[P_2'\left(\frac{x}{r}\right)-\frac{3}{4}\frac{r^2}{a^2}P_4'\left(\frac{x}{r}\right)+\frac{3\times 5}{4\times 6}\frac{r^4}{a^4}P_6'\left(\frac{x}{r}\right)-\ldots\right] \tag{19}$$

where $P_n'(\mu)=\frac{\partial}{\partial\mu}P_n(\mu)$

Values are tabulated in reference 10 or reference 8, page 196, or they may be calculated from series, as above.

For points at a considerable distance from the center of the circle,

$$H_{x(\text{circle})}=\frac{2\pi NI}{10r}\left[\frac{a^2}{r^2}P_2\left(\frac{x}{r}\right)-\frac{3}{2}\frac{a^4}{r^4}P_4\left(\frac{x}{r}\right)+\frac{3\times 5}{2\times 4}\frac{a^6}{r^6}P_6\left(\frac{x}{r}\right)-\ldots\right] \tag{20}$$

$$H_{r(\text{circle})}=\frac{\pi NIy}{10r^2}\left[\frac{a^2}{r^2}P_2'\left(\frac{x}{r}\right)-\frac{3}{4}\frac{a^4}{r^4}P_4'\left(\frac{x}{r}\right)+\frac{3\times 5}{4\times 6}\frac{a^6}{r^6}P_6'\left(\frac{x}{r}\right)-\ldots\right] \tag{21}$$

See reference 11, equations 3 and 4.

For points not far from the axis of the circle

$$H_{x(\text{circle})}=\frac{2\pi NIa^2}{10\rho^3}\left[P_1'\left(\frac{x}{\rho}\right)-\frac{1}{2}\frac{y^2}{\rho^2}P_3'\left(\frac{x}{\rho}\right)+\frac{1\times 3}{2\times 4}\frac{y^4}{\rho^4}P_5'\left(\frac{x}{\rho}\right)-\ldots\right] \tag{22}$$

$$H_{r(\text{circle})}=\frac{2\pi NIa^2y}{10\rho^4}\left[\frac{1}{2}P_2'\left(\frac{x}{\rho}\right)-\frac{1\times 3}{2\times 4}\frac{y^2}{\rho^2}P_4'\left(\frac{x}{\rho}\right)+\frac{1\times 3\times 5}{2\times 4\times 6}\frac{y^4}{\rho^4}P_6'\left(\frac{x}{\rho}\right)-\ldots\right] \tag{23}$$

where

$$\rho^2=x^2+a^2 \tag{24}$$

Equations 22 and 23 are equivalent to equations 9 and 10, reference 1, page 248, volume 2, edition of 1893 and page 212, edition of 1921, changing $1{,}680x^4$ to $1{,}680a^2x^4$.

Formulas for Short Coils

All these formulas for circles can be integrated, though not always by one direct step, to give expressions for the flux density at a point in the end plane of a solenoid, each applicable to a certain range, as approximately indicated in Figure 5. Each formula may be used in an area in Figure 5 in which its number, such as equation 25, occurs, and up to the boundary marked by an arrow leading from that number. The areas are seen to overlap. Satisfactory convergence may be found beyond the boundaries marked, and, on the other hand, the rapidity of convergence may be very poor for the thickness correction formulas near the boundaries. Figure 5 is a preliminary guide, and the criterion for use of a certain formula in any given case is the rapidity of convergence of the series. If the convergence is not suitably rapid, the formula should not be used in that particular case.

By integrating equations 17 and 19 from 0 to x, for values of $r=\sqrt{x^2+y^2}$ less than approximately $0.7a$ (see Figures 3 and 5),

$$H_{x(\text{coil})}=\frac{2\pi nI}{10}\left[\frac{r}{a}P_1\left(\frac{x}{r}\right)\left\{1+\frac{t^2}{12a^2}+\frac{t^4}{80a^4}+\frac{t^6}{448a^6}+\ldots\right\}-\frac{1}{2}\frac{r^3}{a^3}P_3\left(\frac{x}{r}\right)\times\left\{1+\frac{t^2}{2a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{1}{16}\frac{t^6}{a^6}+\ldots\right\}+\frac{1\times 3}{2\times 4}\frac{r^5}{a^5}P_5\left(\frac{x}{r}\right)\left\{1+\frac{5}{4}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{15}{32}\frac{t^6}{a^6}+\ldots\right\}-\frac{1\times 3\times 5}{2\times 4\times 6}\frac{r^7}{a^7}P_7\left(\frac{x}{r}\right)\times\left\{1+\frac{7}{3}\frac{t^2}{a^2}+\frac{21}{8}\frac{t^4}{a^4}+\frac{33}{16}\frac{t^6}{a^6}+\ldots\right\}\ldots\right]$$

lines per square centimeter (25)

where n = turns per centimeter of axial length of the coil. For dimensions see Figure 3.

$$H_{r(\text{coil})}=\frac{3\pi nIy}{20a}\left[\frac{x^2}{a^2}\left\{1+\frac{t^2}{2a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{1}{16}\frac{t^6}{a^6}+\ldots\right\}-\frac{5}{4}\left(\frac{x^4}{a^4}-\frac{3x^2y^2}{2a^4}\right)\left\{1+\frac{5}{4}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{15}{32}\frac{t^6}{a^6}+\ldots\right\}+\frac{35}{24}\left(\frac{x^6}{a^6}-\frac{15}{4}\frac{x^4y^2}{a^6}+\frac{15}{8}\frac{x^2y^4}{a^6}\right)\left\{1+\frac{7}{3}\frac{t^2}{a^2}+\frac{21}{4}\frac{t^4}{a^4}+\frac{33}{16}\frac{t^6}{a^6}+\ldots\right\}\right] \tag{26}$$

Formulas for $H_{x(\infty)}$ and $H_{r(\infty)}$ are obtained by putting $t=0$. For thick coils, the brackets containing power series in t/a may be replaced by the complete expression

$$\frac{a}{mt}\left\{\left(1-\frac{t}{2a}\right)^{-m}-\left(1+\frac{t}{2a}\right)^{-m}\right\} \tag{27}$$

where m is 0 or an even number. The values of m are 0, 2, 4, 6, in equation 25 and 2, 4, 6, in equation 26. For $m=0$, the binomials may be expanded and m cancelled out before m is put $=0$, thus giving a/t logn a_2/a_1 which may be obtained also by integration of $1/a$. The complete expressions may be used also for extending the formulas.

Formulas for Infinitely Long Coils

It is not permissible to integrate expressions 20 or 21 from the limit $x=0$, or past that point, for small values of y, because a/r would be greater than 1 and the series would be divergent. But the series and their integrals become 0 when x becomes infinitely great, and so it is possible to integrate from x to ∞ and obtain the field density at P due to a coil extending from x to ∞. By subtracting this from the field at P due to a coil extending from 0 to ∞, which may be called $H_{x\infty}$ and $H_{r\infty}$ respectively, one obtains the field due to a coil from 0 to x, at a point in the end plane, the same as for all the other formulas for solenoids listed in this paper.

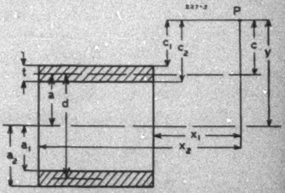

Figure 3. Magnetic field at point *P* near a solenoid

The axial component of field at a point P outside a solenoid which extends to an infinite distance in both directions from P is 0, as is well known. Such a coil is the limiting condition of a toroidal coil of very large coil diameter compared to the section diameter. The parts of the solenoid to the right and left of the radial plane through P give equal axial fields, which are therefore 0. Thus

$$H_{x\infty}=0 \tag{28}$$

for points outside the solenoid.

The axial field inside a coil extending to infinity in both directions is also well known to be $4\pi nI/10$, a constant. By taking a radial plane through P, the axial field due to the half coil to the left of P is by symmetry equal to

$$H_{x\infty}=\frac{2\pi nI}{10} \tag{29}$$

For values of y which lie between a_1 and a_2, Figure 3, the value of $H_{x\infty}$ is that due to the turns lying outside of y. In such a case

$$H_{x\infty}=\frac{a_2-y}{a_2-a_1}\times\frac{2\pi nI}{10} \tag{30}$$

The value of $H_{r\infty}$ requires more computation, for various values of y. First, for large values of y, the integration of equation 21 from $x=0$ to ∞ is permissible, and gives

$$H_{r\infty}=\frac{\pi nI}{10}\left[\frac{a^2}{y^2}\left(1+\frac{t^2}{12a^2}\right)+\frac{1\times3}{4\times2}\frac{a^4}{y^4}\left(1+\frac{t^2}{2a^2}+\frac{t^4}{80a^4}\right)+\frac{1\times3}{4\times6}\times\frac{3\times5}{2\times4}\frac{a^6}{y^6}\left(1+\frac{5}{4}\frac{t^2}{a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{t^6}{448a^6}\right)+\frac{1\times3\times5}{4\times6\times8}\times\frac{3\times5\times7}{2\times4\times6}\frac{a^8}{y^8}\left(1+\frac{7}{3}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{t^6}{16a^6}+\frac{t^8}{2{,}304a^8}\right)+\ldots\right] \tag{31}$$

See equation 40. The series in t/a are not infinite series, but are complete. The general expression is given in equation 41.

For convenience in computation, the numerical coefficients of powers of a/y in equation 31 are 1, $^3/_8$, $^{15}/_{64}$ and $^{175}/_{1024}$.

For small values of y, integration of equation 23 from $x=0$ to ∞ gives

$$H_{r\infty}=\frac{\pi nIy}{10a}\left[1+\frac{3}{8}\frac{y^2}{a^2}+\frac{15}{64}\frac{y^4}{a^4}+\frac{175}{1{,}024}\frac{y^6}{a^6}+\ldots+\frac{t^2}{a^2}\left(\frac{1}{12}+\frac{3}{16}\frac{y^2}{a^2}+\frac{75}{256}\frac{y^4}{a^4}+\frac{1{,}225}{3{,}072}\frac{y^6}{a^6}+\ldots\right)+\frac{t^4}{a^4}\left(\frac{1}{80}+\frac{9}{128}\frac{y^2}{a^2}+\frac{105}{512}\frac{y^4}{a^4}+\frac{3{,}675}{8{,}192}\frac{y^6}{a^6}+\ldots\right)+\text{terms in } t^6/a^6 \text{ and so on}\right] \tag{32}$$

For thicker coils

$$H_{r\infty}=\frac{\pi nIy}{10t}\left[\operatorname{logn}\frac{a_2}{a_1}+\frac{3}{16}y^2\left(\frac{1}{a_1^2}-\frac{1}{a_2^2}\right)+\frac{15}{256}y^4\left(\frac{1}{a_1^4}-\frac{1}{a_2^4}\right)+\frac{175}{6{,}144}y^6\times\left(\frac{1}{a_1^6}-\frac{1}{a_2^6}\right)+\ldots\right] \tag{33}$$

For values of y not very different from a, consider the mutual inductance of two infinitely thin coaxial solenoids of lengths b_1 and b_2 and with a distance between their adjacent end planes equal to w, as in Figure 4. The mutual inductance is given by equation 1, reference 5, as follows:

$$\frac{M}{nn'}=F(x1)-F(x2)-F(x3)+F(x4) \tag{34}$$

where, in the notation used in Figure 4

$x_1=b_1+b_2+w$
$x_2=b_1+w$
$x_3=b_2+w$
$x_4=w$

and where n and n' are the turns per centimeter of the two coils.

Assume that w is a small quantity, and that b_2 is smaller still, so that the right-hand coil is equivalent to a turn of very fine wire. Then $F_{(x1)}$ and $F_{(x2)}$ are to be computed by equation 7 and $F_{(x3)}$ and $F_{(x4)}$ by equation 6 of reference 5. Allow w to increase a small amount. Then, as in equation 7 of this paper, the radial flux density at the circumference of the right-hand coil is

$$-\frac{1}{\pi Dn'b_2}\frac{\partial M}{\partial w} \tag{35}$$

Differentiating $F_{(x1)}-F_{(x2)}$ with respect to w or x, and expanding the first few terms in powers of $1/b_1$, it is found that the result is 0 when b_1 becomes infinite. Differentiating

$$\frac{F_{(x3)}-F_{(x4)}}{\pi Dn'b_2}$$

and discarding higher powers of b_2 and w, the result, excepting the terms in c^8, is

$$H_{r(s)\infty}=\frac{nI}{10}\frac{\sqrt{d}}{\sqrt{D}}\left[\left(\operatorname{logn}\frac{16Dd}{c^2}\right)\left(1+\frac{3}{4}\frac{c^2}{Dd}-\frac{15}{64}\frac{c^4}{D^2d^2}+\frac{35}{256}\frac{c^6}{D^3d^3}-\frac{1{,}575}{128^2}\frac{c^8}{D^4d^4}\cdots\right)-4-\frac{1}{2}\frac{c^2}{Dd}+\frac{31}{64}\frac{c^4}{D^2d^2}-\frac{247}{768}\frac{c^6}{D^3d^3}+\frac{7{,}795}{128\times256}\frac{c^8}{D^4d^4}\cdots\right] \tag{36}$$

The process of taking the same function of w and $w+b_2$, subtracting and dividing by the small quantity b_2 is equivalent to differentiating with respect to w. Expression 36 is therefore the result of differentiating $F_{(x)}$ twice. But $F_{(x)}$ was the result of integrating equation 16, reference 4, twice. Equation 36 should therefore correspond to equation 16, reference 4, which it does, and so the two terms in c^8 can be added from the earlier publication.

The following correction for thickness may be added to equation 36:

$$\Delta H_{r\infty}=\frac{nI}{10}\left[\left(\operatorname{logn}\frac{16D^2}{c^2}\right)\frac{t^2}{D^2}\left(\frac{1}{12}+\frac{1}{4}\frac{c}{D}+\frac{17}{32}\frac{c^2}{D^2}+\frac{95}{96}\frac{c^3}{D^3}\cdots\right)+\frac{t^2}{c^2}\left(\frac{1}{3}+\frac{1}{3}\frac{c}{D}+\frac{1}{4}\frac{c^2}{D^2}-\frac{1}{12}\frac{c^3}{D^3}-\frac{157}{192}\frac{c^4}{D^4}-\frac{1{,}271}{576}\frac{c^5}{D^5}\cdots\right)\right] \tag{37}$$

Since terms in t^4 and higher powers of t are omitted in equation 37, the following formula for thick coils may be used instead of 36 and 37:

$$H_{r\infty}=\frac{nIc}{10t}\left[\left(\operatorname{logn}\frac{16D^2}{c^2}\right)\left(1-\frac{1}{2}\frac{c}{D}+\frac{1}{12}\frac{c^2}{D^2}+\frac{1}{16}\frac{c^3}{D^3}+\frac{17}{320}\frac{c^4}{D^4}+\frac{19}{384}\frac{c^5}{D^5}\cdots\right)-2+\frac{1}{2}\frac{c}{D}+\frac{5}{9}\frac{c^2}{D^2}+\frac{11}{96}\frac{c^3}{D^3}+\frac{7}{4{,}800}\frac{c^4}{D^4}-\frac{71}{1{,}440}\frac{c^5}{D^5}\cdots\right]_{c=c_1}^{c_2} \tag{38}$$

If y is less than a_1, Figure 3, then c_1 and c_2 are negative. If y has a value between that of a_1 and a_2, then c_1 is negative and c_2 is positive.

Formulas for Long Coils

By an integration of equation 20 from $x=x$ to ∞, there is obtained the following formula for the axial flux density at a point P in the end plane of a solenoid of length x, for cases in which $r=\sqrt{(x^2+y^2)}$ is greater than about 1.25a (see Figures 3 and 5):

$$H_{x(\text{coil})}=H_{x\infty}-\frac{\pi nI}{10}\left[\frac{a^2}{r^2}P_1\left(\frac{x}{r}\right)\left\{1+\frac{t^2}{12a^2}\right\}-\frac{3}{4}\frac{a^4}{r^4}P_3\left(\frac{x}{r}\right)\left\{1+\frac{t^2}{2a^2}+\frac{t^4}{80a^4}\right\}+\frac{3\times5}{4\times6}\frac{a^6}{r^6}P_5\left(\frac{x}{r}\right)\left\{1+\frac{5}{4}\frac{t^2}{a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{t^6}{448a^6}\right\}-\frac{3\times5\times7}{4\times6\times8}\frac{a^8}{r^8}P_7\left(\frac{x}{r}\right)\left\{1+\frac{7}{3}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{t^6}{16a^6}+\frac{t^8}{2{,}304a^8}\right\}\cdots\right] \tag{39}$$

See reference 12. The value of $H_{x\infty}$ is given by equations 28, 29, or 30, depending on the value of y.

By integrating equation 21 from $x=x$ to ∞, the corresponding formula for radial flux density is obtained:

$$H_{r(\text{coil})}=H_{r\infty}-\frac{\pi nIy}{10a}\left[\frac{a^3}{r^3}P_1'\left(\frac{x}{r}\right)\times\left\{1+\frac{t^2}{12a^2}\right\}-\frac{1}{4}\frac{a^5}{r^5}P_3'\left(\frac{x}{r}\right)\left\{1+\frac{t^2}{2a^2}+\frac{t^4}{80a^4}\right\}+\frac{1\times3}{4\times6}\frac{a^7}{r^7}P_5'\left(\frac{x}{r}\right)\left\{1+\frac{5}{4}\frac{t^2}{a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{t^6}{448a^6}\right\}-\frac{1\times3\times5}{4\times6\times8}\frac{a^9}{r^9}P_7'\left(\frac{x}{r}\right)\left\{1+\frac{7}{3}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{t^6}{16a^6}+\frac{t^8}{2{,}304a^8}\right\}\cdots\right] \tag{40}$$

where $r^2=x^2+y^2$. See reference 11, equation 5.

The value of $H_{r\infty}$ is given by equations 31–38 and sometimes two of these formulas can be used to check each other. If in Figure 3, $x_1^2+y^2$ and $x_2^2+y^2$ are so large that equation 40 is used for both, then it is evident that in the subtraction of the two results $H_{r\infty}$ cancels out and so does not need to be computed.

The general expression for the brackets in t is

$$\frac{a}{mt}\left\{\left(1+\frac{t}{2a}\right)^m-\left(1-\frac{t}{2a}\right)^m\right\} \tag{41}$$

where $m=3$, 5, 7, and 9 for equations 39 and 40 as far as shown. They are not infinite series, but are complete.

From equations 22 and 23

$$H_{x(\text{coil})}=H_{x\infty}-\frac{2\pi nI}{10}\left[1-\frac{x}{\rho}-\frac{a^2}{\rho^2}\times\right.$$
$$\left\{\frac{1}{2\times2}\frac{y^2}{\rho^2}P_2'\left(\frac{x}{\rho}\right)-\frac{1\times3}{2\times4\times4}\frac{y^4}{\rho^4}P_4'\left(\frac{x}{\rho}\right)+\right.$$
$$\left.\frac{1\times3\times5}{2\times4\times6\times6}\frac{y^6}{\rho^6}P_6'\left(\frac{x}{\rho}\right)\ldots\right\}+\frac{t^2x}{\rho^3}\times$$
$$\left\{\frac{1}{24}\frac{x^2}{\rho^2}-\frac{1}{12}\frac{a^2}{\rho^2}-\frac{y^2}{\rho^2}\left(\frac{1}{16}\frac{x^4}{\rho_4}-\right.\right.$$
$$\left.\left.\left.\frac{21}{32}\frac{x^2a^2}{\rho^4}+\frac{3}{8}\frac{a^4}{\rho^4}\right)\ldots\right\}\right] \tag{42}$$

$$H_{r\,(\text{coil})}=H_{r\infty}-\frac{\pi nIa^2}{10\rho^2}\left[\frac{y}{\rho}P_1'\left(\frac{x}{\rho}\right)-\right.$$
$$\frac{1}{4}\frac{y^3}{\rho^3}P_3'\left(\frac{x}{\rho}\right)+\frac{1\times3}{4\times6}\frac{y^5}{\rho^5}P_5'\left(\frac{x}{\rho}\right)-$$
$$\frac{1\times3\times5}{4\times6\times8}\frac{y^7}{\rho^7}P_7'\left(\frac{x}{\rho}\right)+\ldots+\frac{t^2y}{a^2\rho}\left(\frac{1}{12}\frac{x^4}{\rho^4}-\right.$$
$$\left.\frac{11}{24}\frac{x^2a^2}{\rho^4}+\frac{1}{12}\frac{a^4}{\rho^4}\right)-\frac{t^2y^3}{a^2\rho^3}\left(\frac{1}{8}\frac{x^6}{\rho^6}-\right.$$
$$\left.\left.\frac{17}{8}\frac{x^4a^2}{\rho^6}+\frac{159}{64}\frac{x^2a^4}{\rho^6}-\frac{3}{16}\frac{a^6}{\rho^6}\right)\ldots\right] \tag{43}$$

where $\rho^2=x^2+a^2$

Since terms in t^4 and higher powers of t have been omitted from equations 42 and 43, the following equations may be used for thick coils:

$$H_{x(\text{coil})}=H_{x\infty}-\frac{2\pi nI}{10}\left[1-\frac{x}{t}\left\{\text{logn}\,\frac{a_2+\rho_2}{a_1+\rho_1}+\right.\right.$$
$$\left(\frac{1}{4}\frac{y^2}{x^2}-\frac{5}{16}\frac{y^4}{x^4}\right)\left(\frac{a_2^3}{\rho_2^3}-\frac{a_1^3}{\rho_1^3}\right)+$$
$$\frac{33}{64}\frac{y^4}{x^4}\left(\frac{a_2^5}{\rho_2^5}-\frac{a_1^5}{\rho_1^5}\right)-\frac{15}{64}\frac{y^4}{x^4}\left(\frac{a_2^7}{\rho_2^7}-\frac{a_1^7}{\rho_1^7}\right)+$$
$$\left.\left.\text{terms in higher powers of } y\right\}\right] \tag{44}$$

$$H_{r(\text{coil})}=H_{r\infty}-\frac{\pi nIy}{10t}\left[\text{logn}\,\frac{a_2+\rho_2}{a_1+\rho_1}-\right.$$
$$\frac{a_2}{\rho_2}+\frac{a_1}{\rho_1}-\left(\frac{1}{2}\frac{y^2}{x^2}-\frac{5}{8}\frac{y^4}{x^4}\right)\left(\frac{a_2^3}{\rho_2^3}-\frac{a_1^3}{\rho_1^3}\right)+$$
$$\left(\frac{3}{8}\frac{y^2}{x^2}-\frac{27}{16}\frac{y^4}{x^4}\right)\left(\frac{a_2^5}{\rho_2^5}-\frac{a_1^5}{\rho_1^5}\right)+\frac{105}{64}\frac{y^4}{x^4}\times$$
$$\left(\frac{a_2^7}{\rho_2^7}-\frac{a_1^7}{\rho_1^7}\right)-\frac{35}{64}\frac{y^4}{x^4}\left(\frac{a_2^9}{\rho_2^9}-\frac{a_1^9}{\rho_1}\right)+$$
$$\left.\text{terms in higher powers of } y\right] \tag{45}$$

where $\rho_1^2=x^2+a_1^2$ and $\rho_2^2=x^2+a_2^2$.

See also reference 1, edition of 1921, equation 28, page 222 (change $5a$ to $5a^2$) and equation 31, page 225. Note that $r_1^2=a^2+(x+b/2)^2$ and $r_2^2=a^2+(x-b/2)^2$ where b is the length of the solenoid.

The following are useful formulas for zonal harmonics:

$P_0(\mu)=1;\quad P_1(\mu)=\mu;$

$P_2(\mu)=\dfrac{1}{2}(3\mu^2-1);$

$P_3(\mu)=\dfrac{1}{2}(5\mu^3-3\mu);$

$P_4(\mu)=\dfrac{1}{2\times4}(5\times7\mu^4-2\times3\times5\mu^2+1\times3);$

$P_5(\mu)=\dfrac{1}{2\times4}(7\times9\mu^5-2\times5\times7\mu^3+3\times5\mu);$

$P_0'(\mu)=0;\quad P_1'(\mu)=1;$

$P_2'(\mu)=3\mu;\quad P_3'(\mu)={}^1\!/_2(3\times5\mu^2-1\times3);$

$$P_4'(\mu)=\frac{1}{2}(5\times7\mu^3-3\times5\mu) \tag{46}$$

It is to be noticed that in the formulas, ratios of dimensions occur almost entirely and in such ratios, dimensions in inches may be used throughout instead of dimensions in centimeters, since powers of 2.54, the conversion factor, would occur equally in the numerator and denominator of a ratio and would cancel out. However, the letter n means turns per centimeter and not turns per inch. The letter N means turns per coil.

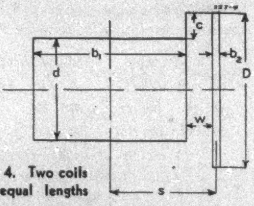

Figure 4. Two coils of unequal lengths

The laboratory measurements which were made agreed with calculated values within a very few per cent.

Example I. The following case, near to the meeting-point of three boundaries, enables one formula to be checked by the others: Find the radial component of field in the end plane of a solenoid where

$x/a=0.9$, $y/a=0.95$, $t/a=0$
$c/a=-0.05$

By equation 9, $H_{r(s)}=5.36\dfrac{nI}{10}$

By equation 36, $H_{r(s)\infty}=6.26\dfrac{nI}{10}$

By equation 40, $H_{r(s)}=6.26-\pi\times0.95(0.446-0.133-0.033+0.020+0.007\ldots)\dfrac{nI}{10}=5.35\dfrac{nI}{10}$

By equation 43, $H_{r(s)}=6.26-\dfrac{\pi}{1.81}\times(0.706-0.164-0.044+0.020+0.007\ldots)\times\dfrac{nI}{10}=5.35\dfrac{nI}{10}$

Example II. The following problem also can be computed by three different formulas. Find the horizontal component of the field in the end plane of the following solenoid:

$x/a=0.4$, $y/a=0.6$, $t/a=0$
$c/a=-0.4$

By equation 3, $H_{x(s)}=2.90\dfrac{nI}{10}$

By equation 25, $H_{x(s)}=2\pi\dfrac{nI}{10}0.721\,(0.555+0.105-0.004-0.013-0.004)=2.90\dfrac{nI}{10}$

By equation 29, $H_{x(s)\infty}=2\pi\dfrac{nI}{10}$

By equation 42, $H_{x(s)}=\left[2\pi-2\pi\times\left\{1-0.3712-\dfrac{1}{1.16}(0.0864+0.0171+0.0023-0.0001)\right\}\right]\dfrac{nI}{10}=2.90\dfrac{nI}{10}$

Example III. A solenoid consists of a single layer of fine wire, $a=0.971$ inch;

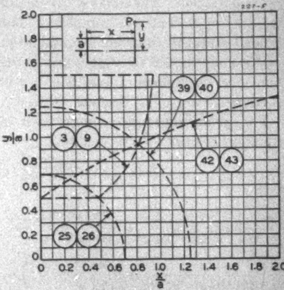

Figure 5. Approximate ranges of application of formulas for magnetic field in end plane of a solenoid

$x=18.25$ inches; $y=0$. The search coil is in the calibrating position, at the center of the solenoid. By equations 29 and 42

$$H_{x(s)}=\frac{2\pi nI}{10}\left[1-1+\left(1+\frac{a^2}{x^2}\right)^{-1/2}\right]=$$
$$\frac{2\pi nI}{10}\left[1-\frac{1}{2}\frac{a^2}{x^2}\ldots\right]=\frac{2\pi nI}{10}[1-0.0014]$$

due to the half coil, which is 0.14 per cent less than the nominal value for a very long solenoid.

It is evident that, for accurate work, the calibration of search coils, or other measurements depending on the magnetic field in the middle of a long solenoid, should include a correction according to the formulas in this paper of the nominal field $4\pi nI/10$ in the middle of the solenoid.

Example IV. Find the radial component of the field in the end plane of the solenoid of example III, at eight inches from the axis.

In this problem, x is equal to the full length of the solenoid, 36.5 inches.

By equation 31, $H_{r(s)\infty}=\dfrac{\pi nI}{10}\times 0.01482$

By equation 43, $H_{r(s)}=$

$$\frac{\pi nI}{10}\,(0.01482-0.00014)$$

Only a very few terms in each series are needed. The turns per centimeter are 7.57 and the current in the test was 1.70 amperes, giving $H_{r(s)}$ by equation 43 $=0.0593$ lines per square centimeter. Test value $=0.0589$ (measurement by M. F. Miller).

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Aircraft Voltage Regulator and Cutout

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Synopsis: In the electrical system on aircraft, the voltage regulator and reverse-current cutout are key pieces of equipment which, in addition to having requirements of utmost dependability, must be small, light in weight, and of a design adaptable to manufacture on a quantity production basis. The conception and simplification of the prime requirements and their attainment by careful analytical designing is described in this paper in such a way as to show not only the steps in design but also the pertinent application features.

REQUIRED: A voltage regulator which weighs less than one-fifth its nearest relative in the industrial field, and a reverse-current cutout weighing less than one-sixth an industrial contactor of equivalent current rating. Such were some general requirements of the generator voltage regulator and cutout whose design and development are described in this paper. This development involved a careful combination of electrical and mechanical engineering principles and necessitated continuous attention being paid to the application requirements. The magnitude of the design problem involved is emphasized by stating that in addition to all those problems normally encountered there are the problems created by the prime requirements of low weight and small size, as well as those created by atmospheric conditions. In this paper the voltage-regulator is taken up first, and this is followed by the reverse-current cutout.

Mr. T. B. Holliday in the paper "Application of Electric Power in Aircraft" (Electrical Engineering, volume 60, May 1941, pages 218–25) gave a very comprehensive picture of the problems and requirements of electric equipment for aircraft, as seen by a member of the materiel division, Wright Field, United States Army Air Corps. A review of that paper provides an excellent foundation for appreciation of specific design problems such as treated in this present paper.

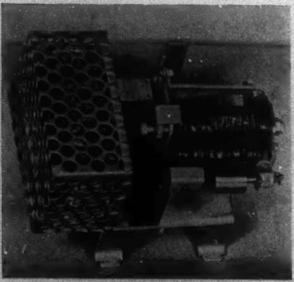

Figure 1. View of voltage regulator showing resistors on left and operating magnet on right

Voltage Regulator

The aircraft voltage regulator described in this paper is shown in Figure 1 and is suitable for operating with any approved 28.5-volt d-c self-excited generator whose maximum field current (at full load and minimum speed) is not more than eight amperes, and whose minimum field current (at no load and maximum speed) is 0.5 ampere. A schematic diagram is shown in Figure 2. Additional specific requirements are:

1. Hold voltage within ±2 per cent of 28.5 volts. This must be maintained over the range from no-load to full-load generator current, while the generator speed may vary from 2,500 to 4,500 rpm.
2. Radio interference must be held to a minimum.
3. Satisfactory operation with air conditions varying over a range of: −40 degrees centigrade to +60 degrees centigrade in temperature, sea level to 35,000 feet in altitude 10 to 90 per cent in relative humidity.
4. Must fit on a standard quick-mounting base which includes all electrical connections.
5. Dimensions including base must not exceed $6^{1}/_{2}$-inch length, 4-inch width, and $3^{7}/_{8}$-inch depth, and of this a total space of $2^{1}/_{2}$ inches by 4 inches by 1 inch must be kept free for mounting base terminal studs and cables.
6. Weight must not exceed 2.5 pounds.
7. Current-carrying capacity of contacts = 15 amperes continuous.
8. Regulator to vary the field resistor between 0.25 ohm and 75 ohms in such a way that generator-voltage surges do not exceed three volts.
9. Regulator must operate satisfactorily with as many as five generators and regulators connected in parallel and provide equal division of load. Maximum deviation from the average value of load must not exceed 10 per cent of generator full-load rating.

The above requirements call for a rather unusual piece of equipment involving major development. It might well be added that neither the attaining of such requirements nor their prior recognition as key characteristics are matters that could be or were settled in a moment or by superficial analysis or study. Rather, the setting down of the requirements and their practical attainment were the result of the engineers of the materiel division keeping a step ahead of the best efforts of the manufacturer's design engineers, thus leading to the final result in a minimum of time.

The voltage-regulator is conveniently divided into four parts in this discussion:

1. Contact device.
2. Operating magnet.
3. Resistor.
4. Base.

These four parts will be treated separately and in that order.

Contact Device

A multicontact type of regulator was chosen as being best fitted to meet the requirements. The Silverstat unit illustrated in Figure 3 is an industrial type of multicontact device which has proven itself by years of service on innumerable applications. It consists essentially of a number of spring bronze leaves with built-in silver contacts at the movable end of each leaf. At the stationary or clamp end of the assembly short strips are used between each leaf to both insulate and space them from each other. The movable ends of the leaves lie against an insulation block whose face is cut at an angle such that, in the free position, all contacts are open and equally spaced. The opening force on each contact is dependent upon the

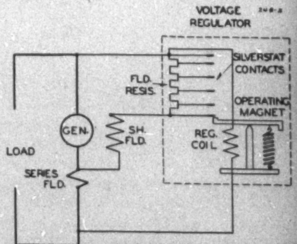

Figure 2. Schematic diagram of voltage regulator

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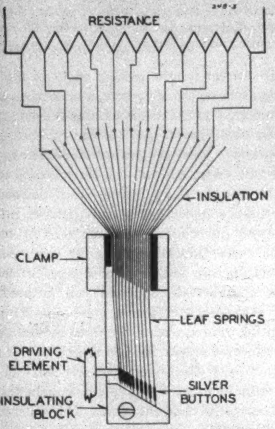

Figure 3. Industrial-type Silverstat unit

tension in its own leaf. Since the leaves are spaced approximately $^{1}/_{64}$ inch apart, and since accurate response to small changes in voltage is necessary, the leaf material must be straight and must have very consistent spring characteristics. On the standard Silverstat assembly, which has been in production for industrial applications for some years, it has been found necessary to maintain a very careful control of the leaf material in order to insure proper leaf characteristics. The standard leaf material, however, was impractical for the aircraft regulator, as markedly higher conductivity and shorter lengths of leaves operating in a wider range of ambient temperatures were required. In view of this, the successful quest for a new material was a metallurgical achievement of no small moment to the designer.

Other factors and problems entering into the design of the contact unit were those connected with the generator itself. In the first place the generators to be regulated were themselves in the development stage. Calculated generator characteristics had to be checked, and the designs changed to be suitable for operation in a regulated voltage system. Changes were being made in the generator specifications giving new field-current limitations. Initially, consideration was given to limiting the total field current to 12.5 amperes and allowing the use of two field circuits both on the generator and the regulator. Final specifications limited the field current to eight amperes and required that the regulator use only one circuit which would vary the field resistor from 75 ohms to 0.25 ohm in steps that would not produce surges higher than three volts at the generator terminals.

A double-deck construction of the contact assembly is used to obtain the necessary number of contact leaves in minimum space. This also reduces the movement required between the all-open and all-closed position of the contacts to one-half of the standard construction which has all leaves in line. The full number of steps is obtained by alternating the operation of the contact leaves in the upper and lower stacks. The mechanical assembly of this double-decked Silverstat is accomplished by the preassembly of the leaves and insulation in a fixture which establishes the correct relation of parts and cements them together. After being baked to fix the cement, these stacks are placed in the double-deck frame and adjusted to proper relation and spring tension. Each Silverstat assembly is checked in a pressure-measuring device and adjusted to have the same corresponding leaf in each stack operate at the same pressure. This is necessary to obtain consistent results required in quantity production.

OPERATING MAGNET

The operating magnet supplies the activating force for closing or releasing as many leaves and steps as is required to establish a voltage within the chosen limits. It must respond to small changes in voltage, moving only enough to readjust the field circuit resistance to the proper value, and it must be small and of such form as to fit in compactly with the other units. Temperature effects must be compensated for, and residual magnetism kept to a minimum. The moving member must be shockproof.

A clapper or moving-iron type is the form of magnet chosen for this application. See Figure 1 which shows the armature at the left of the coils. Two long slender coils are employed in order to obtain the lowest winding weight. The stationary part of the magnetic circuit is arranged in the form of a *U* with a coil on each leg. The armature which completes the circuit is pivoted parallel to the line of the two coils. To reduce the number of parts and obtain a unit assembly, the pivot support is made by extending the flat section of iron connecting the two cores, parallel to the coils to a point approximately even with the open end of the magnetic circuit, then bending this extension at right angles. To minimize weight, the extended section of frame is cut out, leaving only a skeleton form having sufficient mechanical strength. This is possible since the material is not in the magnetic circuit.

To obtain dependable as well as frictionless pivot action, a leaf spring is used. The armature itself is made of a single flat piece of iron cut out around the pivot for free action and extending beyond the pivot to provide for attachment of the calibrating spring and to act as a counterbalance. An insulation angle is attached to the coil side of the armature to act as the contact operating member. The stationary end of the calibration spring is supported by a strip of bimetal attached to the frame.

Adjustment of the calibration spring is obtained by a novel scheme using a minimum number of parts, and is best described with the aid of Figure 4. A hole is punched in the stationary support to fit a threaded stud with one side machined flat; a small point is lanced out of the support in proper relation to the hole to engage with notches on the circumference of the adjusting nut. With the regulator magnet completely assembled, the tension of the calibrating spring holds the adjusting nut in engagement with the point, locking it securely. To change the adjustment it is necessary to tip the adjusting nut and stud just enough to disengage them from the locking projection, then rotate the adjusting nut. The nut is made from a standard pinion stock rod and the fineness of adjustment is determined by the number of teeth. The final choice of:

1. Thread for the adjusting stud of the calibrating spring.
2. Teeth on the adjusting nut.

resulted in a design giving 0.05 ounce change in adjustment per notch or tooth.

The design of the calibrating spring is complicated by the building up of the contact assembly load when the spring loading decreases, also by the fact that the contact assembly load is high in proportion to the magnet pull.

With the lightweight clapper-type magnet it is impractical to obtain stable regulator operation without some form of damping. To obtain damping, trial was

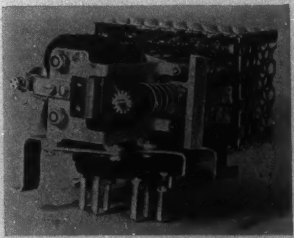

Figure 4. End view of voltage regulator

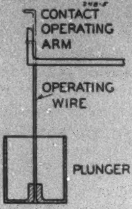

Figure 5(left). Dashpot attachment

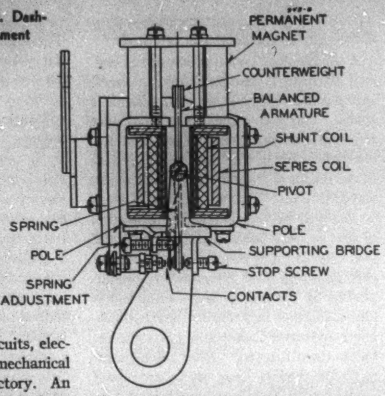

Figure 8 (right). Simplified view of reverse-current relay structure showing balanced armature and location of polarizing magnet

made of electrical damping circuits, electrodynamic dampers, and mechanical dampers, but none was satisfactory. An air dashpot was tried and found to have very desirable characteristics, and it gave excellent results. Both the piston and plunger of this dashpot are made of stainless steel. This minimizes the effects of differential expansion due to temperature, and of corrosion.

Because the end of the armature moves in an arc, and the dashpot plunger in a straight line, it was necessary to provide a connection between them which would not set up a side pressure on the stationary cylinder. Common practice is to include a universal joint in an operating-rod assembly of this type, but such construction is subject to friction and play, both of which are detrimental to accurate regulation. To avoid these faults a long piece of spring wire is employed as a column, one end being soldered in the plunger and the other end slipped through a close-fitting hole in the contact-operating arm. See Figure 5. This same end is bent at a right angle and is sprung to go into another close-fitting hole in the angle of the arm. This results in a tight connection with minimum weight and number of parts.

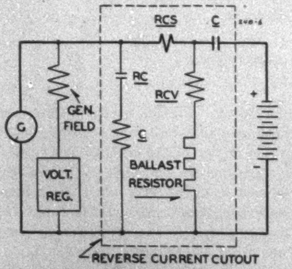

Figure 6. Schematic diagram of reverse-current cutout

Figure 7. Reverse-current cutout

To allow maximum tolerance on the plunger and cylinder diameters, and thus facilitate manufacture, a needle valve is provided for adjusting the restraining effect of the dashpot. The cylinder is mounted on a stud extension from the main body, and the needle valve located inside this stud, thus adding only one part and no weight. Friction is minimized by putting a mirror-smooth polish on the outside of the plunger and the inside of the cylinder, and by use of a carefully chosen lubricant.

Because of the wide variation in operating temperature encountered by aircraft, considerable thought was given to the best method of compensating for the temperature range produced by both ambient change and coil heating. The temperature effect in this regulator is limited by winding the coils with an alloy wire having a temperature coefficient of approximately one-eighth that of copper and a specific resistance of six times that of copper. This gives heavier coils but eliminates the use of an external resistor. It also eliminates the problems of:

1. Finding a negative temperature coefficient resistor to suit this application.
2. A construction for keeping the temperatures of the two parts together so that there is a minimum time lag in correcting for coil heating and cooling.

To correct for the smaller effect of temperature on the alloy wire, a bimetal strip mounted on the magnet frame is used for the stationary support of the calibrating spring. This is located between the points where the two coil cores attach to the frame. Heat transfer from the coil windings to the cores and frame is improved by winding the coils directly on the cores, thus eliminating the heavy insulating tube and air space required where a separate self-contained coil is used. The bimetal strip reduces the calibrating spring tension with a rise in either coil or ambient temperature. It is necessary to co-ordinate the bimetal support and calibrating-spring design closely to gain the ultimate in compensation. The scheme used provides regulation within the required limits under all operating conditions.

Resistor Design

The field resistor presents two separate problems. The first is to obtain a form that will be light enough, work into a compact unit and have taps which are accessible for wiring. The problem was solved only by the wholehearted co-operation of the resistor manufacturer. The original estimate of the resistor weight for the 200-ampere generator was one pound, leaving $1^1/_2$ pounds for the regulator mechanism and mounting base. The final resistor design weighing one-half pound was reached only after intensive study of operating conditions, and by many tests.

The second problem is that of designing the resistor with the right resistance steps to give even voltage response from the generator without overloading the contacts. This requires the co-ordination of generator characteristics with the regulator response and balancing these against practical voltage and volt-ampere limitations of the contacts. The work on this problem was complicated by the changes in generator design and by the requirement that the regulator be satisfactory for regulating any Air Corps approved generators of the 50-, 100- and 200-ampere capacities, without reconnection of the resistor. The characteristics of the various sizes of generators had to be studied, and the resistor steps changed to the best over-all compromise. The resistor steps were of such low value in the high current zone that a ribbon type of resistor material had to be used. This is corrugated after the fashion of the standard Ribflex round resistor tubes, but wound on a flat earthenware form and held in place by ceramic glaze.

MOUNTING BASE

The regulator base presents no special problems, since the other parts are designed as individual units, all mounting from the same flat surface. The base is, therefore, flat except for the special lugs for mounting on the standard subbase. It is cut out for weight reduction and for maintenance purposes and carries a special contact assembly needed to complete electrical circuits to the mounting panel. A perforated metal cover is furnished to protect the resistor.

The complete regulator in its final form as shown in Figures 1 and 4, weighs less than 2.0 pounds, is $5^{15}/_{16}$ inches long, 4 inches high, and $3^{3}/_{4}$ inches deep. It has passed all laboratory tests successfully, operated satisfactorily on test flights and is now in quantity production.

Reverse-Current Cutout

The demand for a reverse-current cutout to handle the 100- and 200-ampere 28.5-volt generators arose coincident with the need for a voltage regulator. In June 1940 the materiel division issued a tentative specification for a 100-ampere device weighing less than two pounds and a 200-ampere device with a 2.5-pound limit. For reasons explained later, the 100-ampere rating has since been abandoned.

While the aircraft cutout performs the same function as the well-known automobile cutout, namely to connect the generator to the battery and load when the generator voltage is sufficient to charge the battery, and to disconnect it when the voltage drops below the charging point, conditions peculiar to the aircraft electrical system demand a higher standard of performance. In particular, the storage battery has a much lower ampere-hour capacity in proportion to the generating capacity, making a low reverse-current dropout setting desirable. Because operation of two or more generators in parallel is usual, a pickup voltage as close as possible to the normal regulated voltage of the generators is necessary to prevent excessive chatter in closing. This requirement results in close specification of pickup voltage tolerances.

The tentative specification called for opening at a reverse-current of not more than five per cent of the continuous rating, and set the pickup voltage between 26.8 and 27.2 volts, or an accuracy of plus or minus three quarters of one per cent. It was further specified that the device should be capable of opening a current equal to 150 per cent of its rating. Maximum dimensions were set at four by four by four inches. These cutouts may frequently be mounted close to the main engines where severe vibration is encountered. To ensure sound mechanical construction a ten-hour vibration test is specified, with a total excursion of 0.07 inch and a frequency varying periodically between 10 and 55 cycles per second.

During the course of the development, modifications of the requirements narrowed down the original problems and introduced new ones. The first addition was the requirement that the cutout should not close on application of reversed voltage. The arc-rupturing capacity of the 200-ampere unit was increased to 500 amperes. An over-all millivolt drop was included and finally whittled down to 85 millivolts at rated current, with the further provision that this must not exceed 95 millivolts after 100 operations of the arc-rupturing test. On the basis of preliminary models submitted by manufacturers, the materiel division reduced the weight limit of the 200-ampere cutout to 2.25 pounds.

Past experience made it clear from the beginning of the development that the measuring and circuit-operating functions should not be combined in one structure, but rather that the design should comprise a relay in addition to a contactor. It is impractical to provide high calibration accuracy in a device which also must carry and efficiently rupture high current. In addition to the greater friction and contact wear effects, the calibration change with temperature cannot readily be controlled without an objectionable increase in weight. The necessity for polarization also influenced the decision to use a separate measuring relay. Figure 6 gives schematically the circuit of such a device consisting of a relay having a shunt voltage-measuring coil *RCV*, a series current coil *RCS*, and contacts *RC* energizing the coil of contactor *C*. Figure 7 shows the cutout in final form.

RELAY

The term "polarization" presupposes the existence of a reference factor of constant direction with which to compare the generator voltage. This factor may be voltage from a battery, the unidirectional conduction of a rectifier, or the fixed polarity of a permanent magnet. For the purpose of a reverse-current cutout, the use of battery voltage as a reference is the most direct line of attack and has the definite advantage of preventing circuit closure when either the battery or generator is reversed. It has the disadvantage of draining current from the battery when the generator is idle. Variations in battery voltage have an adverse effect on the accuracy of calibration.

Use of a rectifier as a reference, while satisfactory for many applications, in general involves a weight handicap. Dry-plate rectifiers, in a case of this kind, must be applied with caution because of the wide range of temperatures which may be expected. High ambient temperature, coupled with normal internal losses, may result in failure. The change in rectifier characteristics with temperature may be reflected in calibration errors, especially at sub-zero temperatures where all dry-plate rectifiers show a large increase in forward resistance. The fact that such rectifiers have a negative temperature coefficient can be used to advantage to balance the positive coefficient of copper windings over a moderate temperature range. Over a wide range this is less practical because the rectifier resistance does not change linearly with change in temperature.

Polarization by a permanent magnet overcomes the disadvantages of the other two methods. Proper application of the better magnetic materials which have been developed in recent years provides a reference polarity of stability adequate to meet the requirements. The proportions of the permanent magnet may be selected so that, with a given permeance of the associated magnetic circuit, the desired flux is obtained with minimum weight. It should be so located as to minimize the demagnetizing effect of external fields, particularly the field of the relay series coil.

The high sensitivity of a polarized relay recommends it for this service. The required sensitivity is determined by the specified reverse-current and the turns in the series coil. Minimum weight demands that the relay operate with a single-turn series coil for the highest current rating for which the frame is intended. Multiple-turn coils may then be used for lower ratings. The relay used in this cutout was designed to have a single-turn coil in the 200-ampere rating, so that with the reverse-current specified as 10 amperes, it must drop out on a 10-ampere-turn reversal as a maximum limit. Reasonable manufacturing and testing tolerances require a design value of six or seven ampere-turns.

For positive contact operation a snap action is necessary in opening and closing. This requires that the armature, in closing, move into a denser magnetic field where the force is greater than that required to move it out of its initial position. The reduction in ampere-turns required to decrease the pull to the drop-out value is produced by the bucking action of the series coil. Experience has shown that a drop-out-to-pickup ratio of 0.8 is near the maximum which will provide satisfactory snap action. If we use this value and a drop-out-to-pickup differential of seven ampere-turns, the correct pickup value will be approximately 35 ampere-turns.

Because of the vibration and shock conditions encountered on aircraft, as well as the high acceleration forces produced by maneuvering and flight in rough air, full static balance is essential on the moving parts of all accurate relays. If the relay structure is not inherently balanced, counterweights must be added even though they mean an objectionable increase in weight. The magnetically polarized relay is easily modified into an inherently balanced mechanical structure by the use of a center-pivoted armature having a working air gap at each end. This requires the addition of sufficient counterweight to balance the weight of the light moving contact only.

Figure 8 shows the details of the relay in its final form. The center-pivoted armature operates between the two *U*-shaped poles which are polarized by the cobalt steel magnet. The armature and poles are made of hydrogen-annealed Hipernik magnetic alloy to minimize hysteresis which would otherwise be objectionable in a low energy relay. To avoid the critical adjustment required in assembling cone pivots without damage or looseness, small diameter polished pin bearings are used. The shaft is 18-8 stainless steel running in bronze bearing screws. A stainless-steel torsion spring with screw adjustment contributes to the shock resistance of the relay and gives latitude of correction for manufacturing variations. A stamped brass bridge supports the armature, spring, and stop screw, so that the whole assembly may be removed after taking out the screws which fasten it to the poles. The combined shunt and series coil spool surrounds the armature and is contained within the poles, the whole assembly being supported from the base by an aluminum mounting frame.

CONTACTOR

The contactor magnetic circuit is of conventional clapper design, but the contact structure is turned around from the usual position in order to place the pivot near the center of gravity. This reduces the armature spring strength required to provide stability under shock, and so somewhat reduces the weight of the magnetic circuit.

When large currents are to be interrupted it is important to remove the arcing from the main contact surfaces to prevent a rise in millivolt drop after repeated operations. Auxiliary arcing tips, opening after the main contacts have separated, are sometimes used for this purpose, but they have several disadvantages. In lightweight devices there is a tendency to make them too weak so as to add as little as possible to the magnet load. They are, therefore, vulnerable to mechanical damage in handling and may not always be effective. Being electrically in parallel to the main contacts they carry a portion of the main current. Gritty dust deposited on the main contacts may cause all of the main current to pass through the arcing tips; a load which they seldom can be designed to carry for more than a short time.

A more satisfactory solution is the conventional rolling contact wherein the final opening always occurs at the tip, leaving the contact heel clean. By varying the kinematic layout, any reasonable value of wipe or scrubbing action may be obtained to keep the contacts clean and break through dust deposits. This is accompanied by a strong toggle action capable of prying apart contacts partially welded by heavy overload. Almost universal use of this construction on industrial contactors indicates its superiority.

The moving contact is an extruded copper shape with a silver-alloy contact face. This part is sufficiently balanced with respect to its pivot so that the spring is able to maintain adequate contact pressure against vibration accelerations in excess of those obtained in the specification test. The flexible copper shunt is brazed directly to the extruded contact by an electrically-controlled spotwelding process using a water spray to prevent annealing or oxidation of the shunt strands. To minimize the bending action as the contactor operates, the point of shunt attachment is located close to the contact pivot.

The silver-alloy-faced stationary contact is carried by a steel extension of the magnet frame, giving a rigid support and making the whole contactor a self-supported unit. The stationary contact structure is so shaped with respect to the moving contact that the current path forms a sharp loop as the contacts separate. This produces a noticeable magnetic blowout action which is particularly useful at currents of 100 amperes and higher.

Instead of using the conventional arrangement with separate armature and contact springs, both functions are performed by one stainless-steel tension spring. Elimination of the extra parts reduces the size and weight and simplifies the assembly. The characteristics of any usual double-spring combination may be essentially duplicated with care in the selection of the factors of spring stiffness, lever arm lengths, and pivot locations. Contact force of one pound is obtained in the sealed position, measured at the contact tip.

Previous to the design of the magnetic circuit, complete magnetic torque data were taken on a somewhat smaller frame having similar proportions. These data were extrapolated to obtain the proportions of a magnet having a torque curve fitting the desired torque curve of the contact and spring system. Tests on a model showed that the first approximation was fairly close, requiring only determination of the best core head and body diameters to obtain the optimum design. The critical portion of the torque-versus-distance curve is the point at which the contacts initially touch. As it is unreliable to depend on kinetic energy to carry the armature over this point, sufficient magnetic torque must be obtained at the minimum design voltage and maximum coil temperature. Best results are obtained with a three-quarter-inch head diameter and a seven-sixteenths-inch core-body diameter. The core end has an integral threaded stud which passes through the frame and is secured by a nut, a construction which eliminates the sharp reduction in core area caused by tapping a fastening screw into the core.

Since it is most economical in weight to limit saturation to the core at the touch point of the torque curve, the frame has a greater cross-sectional area than the core near the junction of the two. The frame area is reduced near the hinge because of the lower leakage flux passing into this portion.

Low reluctance of the unavoidable magnetic gap at the armature hinge is important, since this joint consumes ampere-turns which would otherwise be more usefully employed at the core-head air gap. In addition to minimizing mechanical clearance at this point, an ear is bent down from each side of the armature to utilize the side area of the frame. The magnetic parts are made from low carbon steel and given 925 degrees centigrade annealing in a hydrogen atmosphere to improve the permeability and decrease the residual. The results permit the use of a 0.005-inch-thick residual shim.

With the core diameter and length determined by magnetic considerations, the mean diameter of the coil is determined primarily by the heat dissipation rate. Usual construction methods, involving a coil wound on an insulating tube and slipped loosely over the core, give a high

Analysis of the Application of High-Speed Reclosing Breakers to Transmission Systems

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IN recent years many new or improved kinds of apparatus have been made available for increasing the reliability of transmission circuits. These include high-speed relays, high interrupting speed breakers, ground-fault neutralizers, protector tubes, and high-speed reclosing breakers. Though they may be used in combination, ground-fault neutralizers, protector tubes, and high-speed reclosing breakers offer alternate methods for reducing the outages on the transmission circuit. The reliable loading of a transmission circuit may also be increased by introducing intermediate switching stations and by increasing the interrupting speeds of existing breakers.

Considerable technical-application information has been presented to aid in the general understanding and use of most of this equipment. Although many successful applications of high-speed breaker reclosing have been made in recent years, there still appears to be a real need for an analysis of the general possibilities and limitations of high-speed reclosing of both the three-phase and the single-phase types. This paper attempts to present such an analysis.

General Considerations for High-Speed Reclosing

High-speed reclosing circuit breakers have been shown by experience and analysis to be capable of improving the reliability of transmission systems.^{1,2,3} Where they may be used to best advantage can be fairly well predetermined by stability studies and analysis of test and experience information. Fundamental to the problem are:

1. Maximum permissible time—after the fault has been cleared—for de-energization of the faulted circuit without loss of synchronism.
2. Minimum permissible time for deionization of the fault arc.

The first depends upon the following factors:

a. System arrangement and design.
b. Amount and distribution of generating capacity.
c. Load being carried on faulted circuit and remainder of system.
d. Type, duration, and location of fault.

The second depends upon many factors, among which are:

a. Fault current and its duration.
b. Length of arc.
c. Number of conductors involved.
d. Tower and circuit configuration.
e. Insulator dielectric strength.
f. Weather conditions.
g. Multiple lightning stroke phenomena.

The first fundamental consideration, the maximum time the faulted circuit may be deenergized without loss of synchronism, can be determined by analysis. It is generally recognized that systems sometimes pull back into synchronism even though they may be reclosed together out of synchronism or at

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thermal drop between the two, and depend, therefore, mainly on heat dissipation from the barrel area. By winding the coil directly on the mica-insulated core and impregnating the whole assembly, an excellent thermal joint is obtained, allowing a large portion of the heat to be conducted into the frame and armature for dissipation. The reduction in coil weight by this construction may be as high as 50 per cent, based on designs having equal values of hot ampere-turns and temperature rise as measured by resistance.

The complete contactor, with a rating of 200 amperes and 29 volts continuously, weighs under 14 ounces and is capable of rupturing in excess of the 500 amperes called for in the specification. With its stainless-steel pivot pins operating in bronze bearings it has undergone a mechanical life test of 3,000,000 operations without measurable wear or broken shunt strands.

The contactor and relay designs were co-ordinated to permit combining them on a four-by-four-inch square base with a direct line of connection between the main generator and battery terminal studs so as to minimize the weight of connectors and the over-all millivolt drop, which is less than 75 millivolts at 200 amperes. The base is molded plastic, ribbed for stiffness and minimum weight, and is recessed to keep the main and control terminal posts from turning under wrench forces. Over-all weight of the complete 200-ampere cutout is less than 1.90 pounds. This is under the weight originally specified for the 100-ampere rating. Furthermore, the weights of models submitted by the several manufacturers showed a uniform difference of only a few ounces between the 100- and 200-ampere ratings. Since the weight advantage was so slight the materiel division wisely decided to eliminate the 100-ampere rating and use the 200-ampere size for both the 100- and 200-ampere generators.

Conclusions

Aircraft electrical equipment, such as voltage regulators and cutouts, must meet most of the requirements found in other industrial applications, and in addition must have certain special characteristics. These special characteristics have to do with weight, size, and atmospheric conditions.

While aircraft-application requirements of electrical equipment differ in certain respects from those of other related industrial applications, it is to be expected that the use of equipment specially developed for aircraft service may well be extended to other fields.

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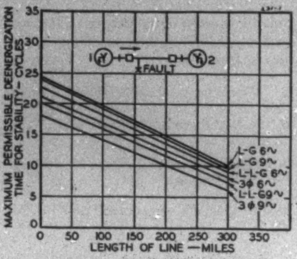


Figure 1. System tie—single-circuit line. Three-phase clearing and reclosing

Sending-end power = $1.1 \times 2.5(kv)^2$. $H_1 = H_2 = 20$ on kilovolt-ampere base of $2.5(kv)^2$. Refer to appendix I and Figure 13

so large an angular displacement that synchronism is temporarily lost. The severity of the consequent disturbance depends upon many factors, but the phenomenon further supports the advisability of quick reclosing. However, operating experience, in general, indicates that it is not desirable even under emergency conditions, to allow for anything but a short interval of nonsynchronous operation.

The time for deionization of an arc is variable and it becomes good practice to allow as much time as possible for the deionization without encroaching too closely upon the stability limits of the system. Information as regards the deionization time is rather meager. The best data are given in a series of papers showing results obtained on the American Gas and Electric Company systems.[1,2,3] These indicate that a time of six to nine cycles for three-phase clearing and 12 cycles for reenergizing results in about 90 per cent successful operation. This gives a datum point of 12 cycles for deionization time at 138 kv, with three-phase clearing and reclosing. Under some conditions the actual time necessary for deionization will be less than 12 cycles, while under severe conditions it may be more. With single-phase switching the arc on the faulted conductors, after the line breakers have opened, tends to be maintained by the capacitive coupling with the sound phase or phases. Longer times are probably required for deionization of the arc path with single-pole switching.[4] See appendix III. It seems evident that the optimum time for de-energization is not necessarily a fixed value but is determined by system conditions. It is hoped and expected that further tests and experience information will be made available by the operating companies.

If the stability margins are small, it may become justifiable to decrease the allowed time for deionization; while if the stability margins are large, it would appear that a longer time could be allowed. An extreme case of a large stability margin (sufficient parallel circuit strength) would require no reclosing at all. Between the two extremes of stability conditions, represented by a single circuit tie and a circuit with many parallel ties, there exist many conditions and cases where high-speed reclosing of some form may be used to advantage. The aim is to obtain improved reliability over transmission circuits for the loads which those circuits are required to carry. This necessitates a proper evaluation of many factors and is an interesting and practical engineering problem for consideration by those responsible for system design and operation.

With the use of generally available a-c network analyzers, calculations are quite readily made to determine the maximum permissible time for de-energization of the faulted circuit. Such a stability study is an important part of the engineering analysis of the feasibility and desired characteristics for reclosing equipments for the particular system under consideration. Fortunately, however, a certain amount of helpful information can also be obtained by analysis of typical system arrangements and conditions. This was done with the use of a network analyzer. The conditions for the cases studied are given in appendixes I and II. Following is a discussion of the results which have been obtained.

Three-Phase Reclosing—Tie Lines and System Interconnections

A series of cases was taken as representative of interconnections or ties between system areas. The systems on either end of the interconnection were assumed to be of equal capacity having fairly low system impedances (five per cent on $2.5(kv)^2$) corresponding to about 1,000,000 short-circuit kva when the transmission line voltage is 138 kv. The line length was varied from 0 to 300 miles in order to show the effect of change in line length. Both systems at either end of the line were assumed to be solidly grounded. The inertia constants were

Figure 2. System tie—100-mile single-circuit line

Three-phase and single-phase switching. Type of switching indicated for each curve by (1φ) or (3φ). Refer to appendix I and Figure 13

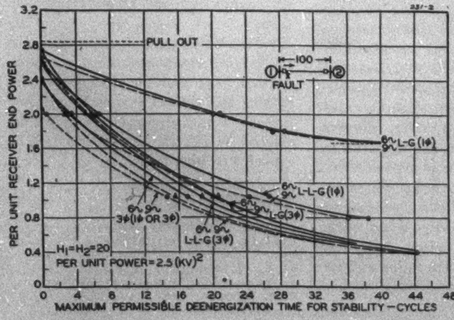
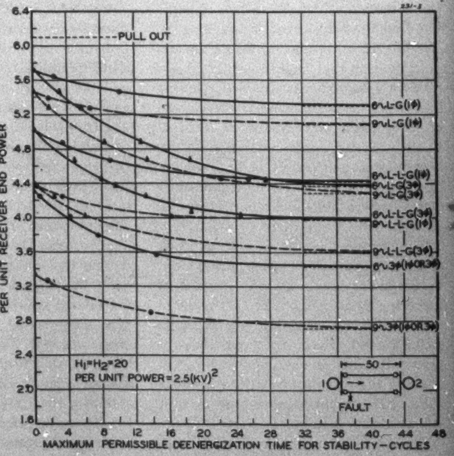


Figure 3. System tie—50-mile double-circuit line

No intermediate switching station. Three-phase and single-phase switching. Type of switching indicated for each curve by (1φ) or (3φ). Refer to appendix I and Figure 13



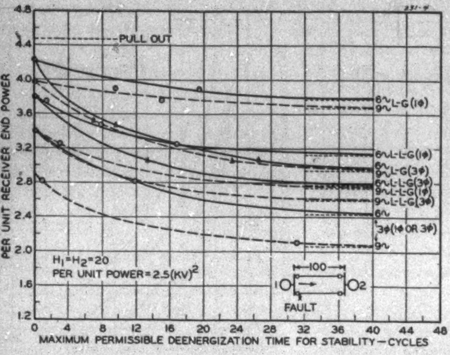


Figure 4. System tie—100-mile double-circuit line

No intermediate switching station. Three-phase and single-phase switching. Type of switching indicated for each curve by (1ϕ) or (3ϕ). Refer to appendix I and Figure 13

Table I. Units of Power for Various Line Voltages

Circuit Voltage—Kv	Unit Power in Kw* = 2.5 (Kv)²
287.5	208,000
230	132,000
161	65,000
138	47,500
115	33,000
69	11,900
34.5	3,000

*Corresponds to about the surge impedance or unity power-factor loading of the line when $\sqrt{L_1/C_1} \cong$ 400 ohms.

taken to correspond to systems having a connected capacity equal to about four times the unity power-factor loading (equal to 2.5(kv)^2) of a single circuit. These represent relatively small systems compared to those which may be found in practice as ties between major system groups. However, the results may be interpreted to correspond to either larger or smaller systems by a corresponding change in the clearing and de-energization times, as explained in appendix I.

Figure 1 shows the maximum permissible de-energization time for stability versus line length for line-to-ground, double-line-to-ground, and three-phase faults at the sending-end terminal for a power transfer at the sending end corresponding to ten per cent above the unity power-factor or surge-impedance loading of the line. At 138 kv this corresponds to $1.1\times 2.5(138)^2 = 53{,}000$ kw. Fault clearing times were taken as six and nine cycles with simultaneous operation at each end of the line. Under these conditions successful operation can be expected for even three-phase faults for line lengths up to 150 miles and 210 miles for a de-energization time of 12 cycles with nine cycles and six cycles clearing, respectively. Successful operation could be expected up to 250 miles for line-to-ground faults with either six or nine cycles clearing. A decrease in the clearing time from nine to six cycles for three-phase faults allows for an increase in the line length of 60 miles for the same de-energization time. It is of interest to note that the type of fault makes an appreciable difference in the allowable de-energization time. These results indicate the importance of keeping the clearing time to a minimum, as this makes possible a further increase in the stability margin of the circuit or an increase in the permissible de-energization times. This has been pointed out previously.[3]

The curves of Figure 1 were drawn for a loading equal to ten per cent above the unity power-factor loading of the line. This is probably the loading to which lines exceeding 150 miles may be loaded. However, for the shorter lengths of line higher loadings may be desired. Fortunately stability characteristics are such as to allow for an increase in loading for shorter lengths with the same de-energization time.

If the sizes of the terminal systems are increased, the resulting increase in the inertia constants produces more stable performance. Such changes can be estimated from Figure 1. For instance if the inertia constants are about double (2.25 times), the six-cycle clearing curve represents the performance with nine-cycle clearing on the new system if the ordinate scale, representing the maximum permissible de-energized time is multiplied by 1.5. The permissible de-energized time for a 150-mile line following the clearing of a three-phase fault in nine cycles is increased from 12 to 22 cycles. Conversely the same load may be transmitted 300 miles, rather than 150 miles, with the same 12 cycles de-energized time.

To illustrate the effect of change of load, the case of 100 miles was analyzed more in detail, results of which are shown on Figure 2. The power in this case is in terms of receiver-end power. The values of per-unit power equal to 1.04 correspond to the case of sending-end power of 1.1 per unit. Table I gives values of per-unit power for various transmission line voltages to be used in the interpretation of the curves.[5] Figure 2 also shows results for single-phase clearing and reclosing. Of particular interest is the very substantial increase in power limit with decrease in the de-energization time with three-phase switching. For three-phase or double-line-to-ground faults, the curves are such

Figure 5. System tie—200-mile double-circuit line

No intermediate switching station. Three-phase and single-phase switching. Type of switching indicated for each curve by (1ϕ) or (3ϕ). Refer to appendix I and Figure 13

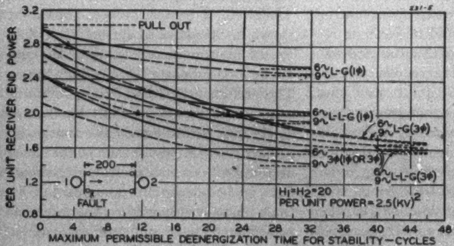
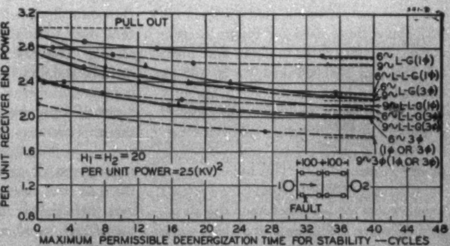


Figure 6. System tie—200-mile double-circuit line with intermediate switching station

Three-phase and single-phase switching. Type of switching indicated for each curve by (1ϕ) or (3ϕ). Refer to appendix I and Figure 13



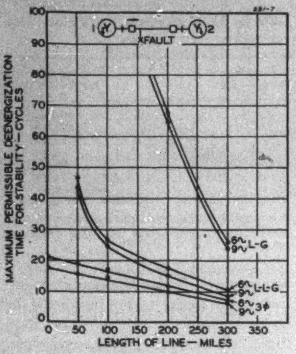


Figure 7. System tie—single-circuit line

Single-phase clearing and reclosing. Sending end power $=1.1 \times 2.5(\text{kv})^2$. $H_1 = H_2 = 20$ on kva base of $2.5(\text{kv})^2$. Refer to Figure 13, Appendix I

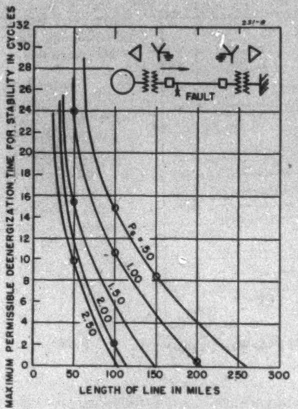


Figure 8. Hydroelectric system—single-circuit line

Single-phase switching of line-to-ground faults. Nine cycles clearing. P_G = rated generator output. Curves are for a loading ten per cent above generator rated output. Refer to Figure 14, appendix II

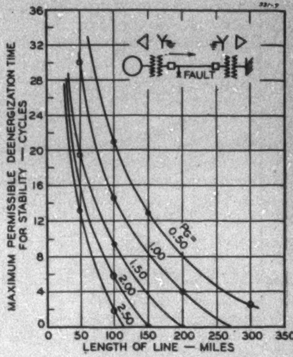


Figure 9. Hydroelectric system—single-circuit line

Single-phase switching of line-to-ground faults. Six cycles clearing. P_G = rated generator output. Curves are for a loading ten per cent above generator rated output. Refer to Figure 14, appendix II

that, in the region of 12 cycles de-energization time, a decrease of three cycles in the de-energization time is equivalent to about a three-cycle reduction in fault-clearing time. This indicates that a cycle reduction in fault-clearing time is equivalent to a cycle of reduction in de-energization time. There is possibly a further improvement with the quick clearing in that the ionization in the arc path does not involve as great a volume of air, and, therefore, a further reduction in deionization time may be allowed. This is of considerable interest because studies of transient stability limits of systems have indicated that there was not much to be gained by reduction in fault clearing times *without* reclosing below eight or possibly five cycles. Figure 2 indicates that with three-phase reclosing, three cycles reduction in time, either clearing or de-energization is worth about 15 per cent increase in power limit.

If the interconnected systems have greater inertias than those used in determining the curves on Figure 2, a correspondingly greater amount of power can be transferred for the same de-energization time. For example, if the inertia constants of the interconnected systems are 2.25 times those used for Figure 2 (generating capacity equal to about nine times 2.5 $(\text{kv})^2$ or 400,000 kva for a 138-kv transmission line) the transient stability limit is increased from a per-unit receiver power of 1.16 to 1.62 with nine-cycle clearing of three-phase faults and a 12-cycle de-energization time. This indicates that the inertias of the interconnected systems have a considerable effect upon the allowable de-energization time and that, when applying reclosing mechanisms, this effect should be considered. It is evident that when the interconnected areas are of a large capacity compared with the tie-line capacity, three-phase reclosing may be entirely adequate, even for single-circuit ties for loads up to the maximum which the circuit will be required to carry.

Figure 4 gives the results for 100 miles of double-circuit line with no intermediate switching station. It will be noted from the bottom curve on this figure that a load corresponding to two per unit (this corresponds to unit power on each circuit) may be carried with no reclosing, following a three-phase terminal fault which is cleared in nine cycles. A power of 2.4 per unit may be carried with no reclosing if six-cycle clearing is used. As shown by Figure 4, the increase in power limit by the use of reclosing is much less than that for a single line, as shown in Figure 2. With 12 cycles de-energization time and nine cycles clearing for a three-phase fault, the power limit is increased about ten per cent over what it would be with no reclosing. It will be noted that the improvement in stability limit for six cycles clearing of the three-phase fault over nine cycles clearing is 40 per cent greater than the improvement obtained by the use of reclosing. These results indicate that for some types of system, depending upon the load it is desired to transfer, there is very little benefit to be obtained from the use of high-speed reclosing. However, if the systems have relatively large inertias, the increase in power limit can be further improved. This indicates that an appreciably greater gain may be obtained with three-phase reclosing on parallel lines when the inertia constants are large; whereas, if the inertia constants are small, it may be impossible to carry the rated load of the circuit. Additional curves are given in Figures 3 and 5 for 50 and 200 miles of double-circuit line, respectively, with no intermediate switching station. Figure 6 corresponds to a 200-mile double-circuit line bussed together at the center. It will be noted that by bussing, the 200-mile line performance is improved, and the gain due to reclosing is decreased, and may be such that it may not be necessary to use any reclosing. For the 50-mile double-circuit line, Figure 3, the curves are relatively flat indicating very little benefit to be derived from reclosing.

For circuits which are on the same tower, more than one circuit may be involved in the fault, and in such a case successful reclosure of even one of the two circuits may prevent loss of synchronism. Therefore, a benefit greater than that shown in the attached curves for double-circuit lines can be realized when the conductors are supported on the same tower. The performance under these conditions would be somewhat similar to that of single-line performance as shown on Figures 1 and 2. Single-line performance or simultaneous faults on double-circuit line can be adequately taken care of by three-phase reclosing when the interconnected areas have sufficient capacity or inertia effect. This has been supported by experience as given in reference 3.

Single-Phase Reclosing—Tie Lines and System Interconnections

Single-phase clearing and reclosing of transmission-line circuits has been considered a possibility for a long time but has been put into operation only to a limited extent. With a line-to-ground fault only one-phase wire need be interrupted, the remaining tie over the two sound phases and ground being sufficiently strong to prevent loss of synchronism for a considerable range of load and system conditions obtained in practice. When two-phase wires are involved, however, the remaining tie is so reduced in strength as to result in but little benefit over three-phase reclosing for the same de-energization times. There is, of course, no benefit if all three-phase wires are involved.

Figure 7 shows the maximum permissible de-energization time in cycles for stability of a single-circuit tie using single-phase clearing and reclosing. These curves are for a loading corresponding to a sending-end power ten per cent above the unity power-factor loading of the line and with the connected systems having a capacity about four times the transmitted power. As shown by these curves, this power can be transmitted up to 150 miles with three-phase switching, nine-cycle clearing, and a 12-cycle de-energization time, for a three-phase fault. It is also of interest to note that the permissible de-energization time for line-to-ground faults is considerably higher than for three-phase or double-line-to-ground faults. This indicates that for line-to-ground faults single-phase reclosing is a very effective way of preventing loss of stability. However, its greatest advantage occurs when the loading is such that three-phase reclosing is no longer adequate. This condition is more likely to exist when the interconnected systems are relatively small.

Figure 2 shows the results for a single-circuit line 100 miles long. This compares the results using single-phase reclosing with three-phase reclosing. If the loading of the line is increased above unity power-factor loading, and the inertias correspond to those used in the figure, the line-to-ground faults may be switched off by the use of single-phase switching without loss of synchronism up to a loading which is 50 per cent greater than that of a line-to-ground fault with three-phase reclosing based on the same de-energization time of 12 cycles. However, if the deionization time for single-phase reclosing must be 50 per cent longer, which may be necessary for a 100-mile line, the improvement is from a per-unit power of 1.5 to 2.05 or about 35 per cent increase in power limit. Similar comparisons can be made by examining the results shown on Figures 3, 4, 5, and 6.

High-Speed Reclosing—Hydroelectric System

Representative of a type of system where reclosing might be applied is a hydroelectric station located at some distance from a large system to which it delivers power. This case of a typical hydroelectric station was studied for various lengths of line and line loadings. The transmission-line constants were assumed to be the same as for the study of interconnections previously discussed. Typical hydroelectric generator constants were used ($x_d'=0.30$, $H=3.0$). The sending- and receiving-end transformer reactances were assumed as ten per cent on the generator rating. The receiving-end system was assumed to have infinite inertia and an equivalent reactance corresponding to ten per cent of the hydroelectric generator rating. See appendix II. Calculations were then made to determine the transient-stability limits with single-line operation and high-speed reclosing. The hydroelectric generators were assumed to have a rated power factor of 0.9 and to be operating at ten per cent above their kilowatt rating, a loading which about corresponds to their kilovolt-ampere rating. The results are therefore on the basis of the total hydroelectric generating capacity being operated at slightly above rated kilowatt load. The results indicate the maximum permissible de-energization times under these conditions.

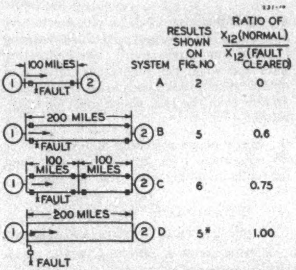


Figure 10. Systems used for interpretation of results in terms of present-day standard breakers shown on Figures 11 and 12

*De-energization times for this case taken to correspond to zero cycles

For this type of system, which has a relatively low inertia constant and which has a transmitted load equal to the total generated capacity at the sending end of the line, three-phase reclosing is impractical unless de-energization times of one to five cycles were possible. Furthermore, single-phase reclosing for anything but line-to-ground faults is impractical, because of the small available time for deionization before stability is lost. However, for line-to-ground faults single-phase reclosing may prove practical. Figures 8 and 9 show the results of calculations for this case with nine- and six-cycle fault clearing, respectively. It will be noted from Figure 8 that a line loading of unity corresponding to 50,000 kw at 138 kv may be transmitted 50 miles, with stability, when nine-cycle clearing and 27-cycle de-energization time are used. From Figure 9, if six-cycle clearing is used, a line loading of unity may be transmitted 55 miles with stability. For heavier line loadings a corresponding reduction in miles to which the power can be successfully transmitted through a line-to-ground fault is obtained.

Calculations were also made to determine the effectiveness of a high-resistance amortisseur winding for increasing the permissible de-energization time when single-phase switching is used. As is well known,[12,13] such a winding will increase the braking torque during a circuit unbalance and may be of advantage under such conditions in reducing the effective accelerating torque. For a 100-mile single-circuit line with a unit loading ($P_G=1.0$), the maximum permissible de-energization time was found to be increased about 35 per cent for line-to-ground faults. Such a winding may, therefore, be effective for a hydroelectric generator connected to its load area by single-circuit line, when it is desired to protect against line-to-ground faults by single-phase switching rather than by a ground-fault neutralizer. If it is necessary to protect against faults involving two or more phases, single-phase switching is not effective, and there is but little benefit to be derived by the use of a high-resistance amortisseur winding.

Summary From Preceding Studies

(*a*). THREE-PHASE RECLOSING ON SYSTEM TIES

1. The benefits to be obtained from reclosing are greatest for single-circuit lines, or simultaneous faults on double-circuit lines.

2. Three-phase reclosing is a very practical means for maintaining the transmission-line loading between interconnected areas when the interconnected areas have generating

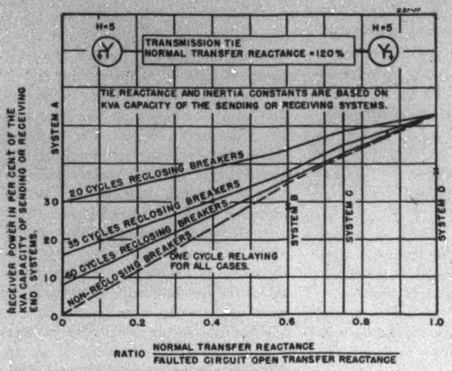


Figure 11. Stability limits for three-phase faults at sending-end line terminal, nine cycles clearing, as a function of reclosing time and ratio of transfer reactances

Refer to Figure 10. The two points marked "x" on the figure show the power limit for unsuccessful reclosure against a three-phase fault on one circuit using 20-cycle reclosing breakers

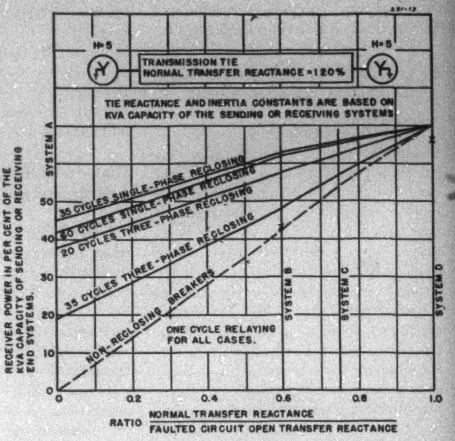


Figure 12. Stability limits for line-to-ground faults at sending-end terminal, nine cycles clearing, as a function of reclosing time and ratio of transfer reactances

Refer to Figure 10. The two points marked "x" on the figure show the power limit for unsuccessful reclosure against a line-to-ground fault on one circuit using 20-cycle three-phase reclosing breakers

capacities of the order of at least four times the transmission-line loading.

3. With three-phase reclosing of single-circuit lines a decrease in the clearing time for three-phase faults is as important as a decrease in de-energization time. Therefore, further reductions in the fault-clearing time directly improve the stability.

4. With parallel circuits, the benefits to be realized from reclosing decrease with decrease in line length and with increase in the number of intermediate switching stations.

(*b*). Single-Phase Reclosing on System Ties

1. Single-phase switching provides increases in transient power limits on single-circuit lines for line-to-ground faults which are not possible with three-phase switching; the difference however, may be small if the interconnected areas have high generating capacity relative to the power to be transmitted.

2. The power limits of a single-circuit line are greater with a ground-fault neutralizer for extinction of line-to-ground faults than those which can be obtained with single-phase switching.

3. If the inertia constants of the interconnected systems or the connected capacities are sufficiently great, the practical advantages of single-phase switching may be negligible.

4. For line-to-ground faults, if single-phase switching is used, the time for de-energization is not particularly critical as regards its effect on the stability limits, and, therefore, it is not necessary to strive for low reclosing time as is the case with three-phase reclosing.

5. The advantage of single-phase switching appears to be important only when reclosing is used on single-circuit interconnecting lines and when the generating capacity of one of the interconnected areas is not much greater than the load which must be transmitted.

6. Single-phase switching appears to offer no advantage over three-phase switching for faults involving more than one phase.

(*c*). Single-Phase Switching for a Hydroelectric Station Delivering Power Over a Single-Circuit to a Large System

1. Single-phase reclosing makes possible the maintenance of stability through self-clearing line-to-ground faults for normal line loadings and for short distances.

2. For longer lines and more heavily loaded circuits, other means such as the ground-fault neutralizer may be required in order that the system may ride through line-to-ground faults.

3. This type of system is not stable with either single-phase or three-phase reclosing of faults which involve more than one phase.

Ground-Fault Neutralizers

Ground-fault neutralizers should be compared with the use of single-phase switching, since the advantages of using the latter means are chiefly limited to line-to-ground faults. The ground-fault neutralizer has an advantage over single-phase switching in that there is no reduction in stability limits for line-to-ground faults. For the shorter lines this may be of no particular advantage over single-phase switching, as the circuit loadings may be well below the transient-stability limits with single-phase switching. However, it may be necessary to retain as much stability margin as possible in order to carry rated circuit loadings on the longer lines. This, therefore, indicates that for long single-circuit lines, ground-fault neutralizers have a distinct advantage over single-phase switching, whereas for the shorter lines and on systems which are necessarily solidly grounded, single-phase switching may have important advantages. When using ground-fault neutralizers on single-circuit lines, one should give attention to the switching and circuit arrangements in order to avoid the possibility of high transient switch voltages. This problem, however, is now well understood,[11] so that the conditions which give rise to such abnormal voltages can be avoided.

Relaying

In general, the use of reclosing on tie circuits requires that the terminal breakers be opened simultaneously in order to obtain the benefit of maximum deionization time to clear the arc path. This essentially requires the use of pilot-wire relays of some form to give the simultaneous tripping at both terminals. For the lines generally considered in this analysis, the line lengths are such that the carrier-current form of pilot relaying would be used. Successful operation of high-speed reclosing with carrier relaying is already a matter of record.[3]

For single-phase operation, it is only necessary to add to the basic carrier pilot-relay equipment phase-selecting relays to select the proper phase or pole to be opened in case of single-phase faults. Since single-phase operation is feasible only on a solidly grounded neutral system, these phase selecting relays can be simple impedance devices operating from line-to-neutral voltage and line residual current. In some cases of single-phase reclosing it may not be necessary to obtain simultaneous tripping at both terminals, because the permissible de-energization time is so great that sequential tripping could be tolerated.

Interpretation in Terms of Present-Day Standard Breakers

Although the previously presented results cover a wide range of possible system conditions and breaker speeds, they may require some interpretation in terms of present-day standard-breaker clearing and reclosing times. It was found that this could be conveniently done by cross-plotting the results for the clearing and de-energization times corresponding to standard breakers for the series of systems shown in Figure 10. All of these four system arrangements have essentially the same transfer reactance for normal conditions, neglecting resistance and capacitive susceptance. If it is assumed that the inertia constants of the two interconnected systems correspond to $H=5$ per unit on the kilovolt-ampere rating of the generator capacity of each system (four times the per-unit power of the preceding studies or $10(\text{kv})^2$), the transfer reactance is 120 per cent on the kilovolt-ampere capacity of each interconnected system. The ratio of the normal transfer reactance to the transfer reactance with the faulted section switched out can be used as an indication of the shock to the system resulting from switching out the faulted section. In the right-hand column of Figure 10 are tabulated these ratios.

(*a*). THREE-PHASE FAULTS

Figure 11 shows the per cent power (based on the generating capacity of the individual interconnected systems) which can be transferred with stability through a three-phase fault at the sending-end line terminal, plotted against the ratio of transfer reactance for the different standard-breaker times. Zero ratio of transfer reactance represents a single-circuit tie, whereas increasing ratios indicate an increasing number of intermediate stations on parallel lines.

In Figures 11 and 12 it is assumed that

(*a*) Relay delay is one cycle at each end of the circuit.

(*b*) Breakers with eight cycles interrupting time rating are used.

The circuit de-energized time is eight cycles less than the reclosing time rating. From Figure 11 several important effects are apparent:

1. The greatest possible gain with reclosing may be realized for systems which approach the single-circuit tie (system *A*, Figure 10).

2. The smallest possible gain is realized for systems which approach a multiple-circuit tie sectionalized by a large number of intermediate switching stations (system *D*, Figure 10). Except for the possibility of simultaneous double-circuit outages, there is little reason for using reclosing breakers in a system of this type. If double-circuit faults can occur, the performance is almost the same as that of the shorter single-circuit line (system *A*, Figure 10).

3. A point of diminishing returns is soon reached for further sectionalizing beyond that represented by the double-circuit tie with one intermediate switching station (system *C*, Figure 10).

4. The advantages of 20-cycle over the slower 35- or 60-cycle reclosing breakers are apparent.

5. If single-phase reclosing is slower than three-phase reclosing, the allowable power transfer during three-phase faults is correspondingly less.

6. The successful power transfer is very appreciably increased even when one strong tie is paralleled by another of relatively low capacity. If two such circuits have equivalent lengths of 150 and 300 miles, the transfer reactance ratio is approximately 0.4 and the allowable power transfer is at least 25 per cent greater than for a single 100-mile circuit.

(*b*). LINE-TO-GROUND FAULTS

Figure 12 shows the performance of the same four systems analyzed in the same manner for line-to-ground faults, using both single-phase and three-phase reclosing breakers. Because a longer deionization time is required for single-phase reclosing than for three-phase reclosing, 35 and 60 cycles were taken as the reclosing times for the former, and 20 and 35 cycles for the latter. The general form of the curves is about the same as for Figure 11. From Figure 12 several important effects are apparent:

1. Appreciably greater power can be transmitted for the single-circuit system with three-phase reclosing, if 20-cycle rather than 35-cycle reclosing is used. A further but much smaller gain is realized by the use of 35-cycle single-phase reclosing.

2. With single-phase reclosing, the difference between 60 and 35 cycles reclosing is quite small. The speed of reclosing for single-phase switching on line-to-ground faults is less important than that for three-phase switching.

(*c*). OTHER CONSIDERATIONS

Figures 11 and 12, though limited to one value of normal transfer reactance (120 per cent) indicate the relative importance of several of the essential factors during line-to-ground and three-phase faults. A similar interpretation for double-line-to-ground faults shows that, where high-speed reclosing is applicable, 20-cycle three-phase reclosing gives appreciably better results than 35-cycle single-phase reclosing. Further interpretations show that, with increase in system inertia, the stability limits with high-speed reclosing are materially increased. This indicates that the use of high-speed reclosing will naturally tend to increase with the trend toward interconnection of large systems by ties of relatively small capacity.

High-speed reclosing involves the risk that occasionally the fault will not be self-clearing, and any attempt to reclose may do harm rather than good. This risk may be evaluated in two ways. First, the decrease in power limit due to an unsuccessful reclosure below that obtained with no reclosing. Second, the decrease in power limit due to an unsuccessful reclosure below that obtained with successful reclosing.

Calculations for both systems *B* and *D* of Figure 10 indicate that quick single-phase switching and reclosing of line-to-ground faults can involve negligible risk of either the first or second kind. The advantage of the possible small risk of the second kind may be largely only of theoretical rather than of practical value because to obtain it would require an unusually long period of unbalanced operation with one phase open. As shown by the points (marked x) on Figure 12, automatic three-phase unsuccessful reclosure against line-to-ground faults involves negligible risk of the first kind but appreciable risk of the second kind. For either single-phase or three-phase unsuccessful reclosure against the more severe faults, double-line-to-ground or three-phase (for three-phase faults points marked x on Figure 11), the risk of both the first and second kinds is appreciable. Calculations made for the case of delayed or manual reclosure showed no risk of the first kind. Calculations were also made for the 200-mile tie with an intermediate bussing station (system *C*) for the special case of a double-line-to-ground fault due to a single-line-to-ground fault on different phases of each circuit. Both circuits were assumed to be cleared in nine cycles, closed after 12 cycles de-energization time, unsuccessful reclosure on one circuit, and with subsequent nine cycles clearing on this faulted circuit. The critical loading for this case with three-phase reclosing was found to be about the same as for a self-clearing three-phase fault on one circuit of system-*A* type. With single-phase reclosing for this case, the critical loading is nearly double the value obtained with three-phase reclosing.

From both Figures 11 and 12, it is apparent that the risks are least where the possible gain from reclosing is greatest.

General Conclusions

1. Under favorable conditions for its use, high-speed reclosing provides a simple means for substantially increasing the system reliability.

2. The choice of the type and available time ratings for reclosing breakers requires an analysis to weigh properly their relative advantages.

3. With three-phase reclosing, de-energization time is important for all types of faults, and clearing time is important for double-line-to-ground and three-phase faults but not for single-line-to-ground faults.

4. For single-phase switching, both clearing and de-energization time are relatively unimportant for single-line-to-ground faults but are important for faults involving more than one phase.

5. Until more is known about the deionization time requirements with single-phase switching, it appears logical to limit single-phase reclosing to line-to-ground faults and to applications where moderate reclosing delay carries no particular stability penalty.

6. High-speed reclosing will show the greatest advantage and will tend to be more generally used as the number of system interconnections and the size of systems increase, relative to the strength of their individual interconnecting ties.

Appendix I. Study of System Interconnections

A series of cases was set up for study of three-phase and single-phase reclosing, having constants which were considered representative for illustrating the factors involved in system interconnections. These cases were studied on an a-c network analyzer. The following representative line constants of a 60-cycle overhead transmission line were used:

x_1 = 0.8 ohm per mile, positive sequence reactance.

r_1 = 0.16 ohm per mile, positive sequence resistance.

$Y_1 = 5.2 \times 10^{-6}$ mhos per mile, positive sequence susceptance.

x_0 = 2.4 ohms per mile, zero sequence reactance.

r_0 = 0.43 ohm per mile, zero sequence resistance.

$Y_0 = 3.12 \times 10^{-6}$ mhos per mile, zero sequence susceptance.

For study of double-circuit lines the zero-sequence mutual capacitance and reactance effects were neglected because

(*a*). These assumptions would apply for lines not on the same right of way.

(*b*). These are secondary effects only and would not modify appreciably the results or conclusions of this study even for lines on the same tower.

The base kilovolt-amperes and kilowatts were taken as 2.5 $(\text{kv})^2$ corresponding to the surge-impedance or unity power-factor loading of a line having the constants selected for study.[5] The sending-end and receiving-end systems were assumed to have equal impedances. The positive-, negative-, and zero-sequence impedances of sending- and receiving-end systems were assumed to be the same and equal to $0.002+j0.05$ per unit on a base kilovolt-ampere equal to 2.5 $(\text{kv})^2$. See Figure 13. The system was set up three-phase on the network analyzer so that the simultaneous dissymmetries produced by opening single-phase switches at either end of the line, with or without a fault on one or more phases, could be more easily represented. Also in this way the capacitive ground current and the fundamental frequency voltage on the cleared conductor would be determined directly for any condition.

The voltages back of system impedances were held fixed for all loadings corresponding to values which would give normal or unit voltage at each line terminal for a power transfer per circuit at the sending end of 1.10 per unit. For higher initial loadings the results give somewhat lower limits than may be actually obtained; whereas for lower initial loadings, the limits are somewhat higher. However, these differences are small because of the comparatively small terminal or system impedances.

The faults were all taken at the sending-end terminal and assumed to be cleared in six or nine cycles. The maximum permissible time for de-energization of the faulted conductor or conductors was determined by calculation.[6] The line length was varied from 0 to 300 miles. Both single- and double-circuit lines with and without intermediate bussing were studied. Some of the results obtained are shown on Figures 1–7. These results are given for equal sending- and receiving-end system inertias of $H=20$ per unit based on 2.5 $(\text{kv})^2$. A typical system may have an inertia constant (H) of 5.0 per unit on its connected kilovolt-amperes of generator capacity. Accordingly, with an $H=20$ on 2.5 $(\text{kv})^2$ and a transmission-line loading of unity, corresponding to unity power-factor transmission, the connected generator capacity of the sending- or receiving-end systems corresponds to about four times the power being transferred.

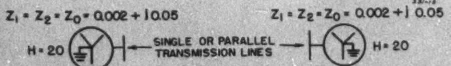

$Z_1 = Z_2 = Z_0 = 0.002 + j0.05$
H = 20
SINGLE OR PARALLEL TRANSMISSION LINES
$Z_1 = Z_2 = Z_0 = 0.002 + j0.05$
H = 20

Figure 13. Typical system interconnection

Refer to appendix I

Since results have been obtained for both six-cycle and nine-cycle clearing, the curves for six-cycle clearing and $H_1=H_2=20$ can be readily interpreted for nine-cycle clearing with $H_1=H_2=45$ by increasing de-energization times given on the curves by a factor of 1.5.

Similarly the nine-cycle clearing results for $H_1=H_2=20$ may be interpreted as applicable for six-cycle clearing with $H_1=H_2=20/2.25=8.86$ if the de-energization times are decreased by dividing by 1.5.

The concept of equivalent or effective system inertia constant may be applied approximately (could be correctly applied if resistance were negligible) so that the above results may be interpreted to apply to systems having effective inertia constants corresponding to $H_0=H_1H_2/(H_1+H_2)$.[7,8]

The pull-out power shown in the curves corresponds to the receiving pull-out power.[9] This may also be used as the power limit for a line-to-ground fault for ground-fault neutralizer operation as compared with the other methods of fault clearing.

Appendix II. Conditions for Study of Hydroelectric Systems

A series of cases was studied corresponding to a hydroelectric station delivering power over a single-circuit line to a large system. The line constants were taken to be the same as for the interconnection study outlined in appendix I. The remaining system constants are, see Figure 14,

x_d' = 0.30 on generator kva

$x_{T1}=x_{T2}$ = 0.10 on generator kva

x_s = 0.10 on generator kva

P_r = kw rating of generator in per unit of 2.5$(\text{kv})^2$

Rated generator power factor = 0.9

Generator kva = $P_r/0.9$

Normal voltage was assumed at the sending- and receiving-end high-voltage terminals under initial conditions. The faults were taken at the sending end.

The maximum permissible de-energization times were determined for a power transfer of $1.1 \times P_r$ corresponding to a ten per cent stability margin or a power practically equal to the kilovolt-ampere rating of the generator. The results of the calculations made for these cases are shown on Figures 8 and 9 for nine- and six-cycle clearing. The maximum permissible deenergization times for faults involving more than one phase with either single-phase or three-phase switching were all less than five cycles.

The results for the six-cycle clearing can

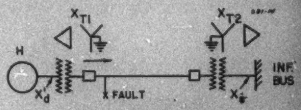


Figure 14. Typical hydroelectric system interconnection

Refer to appendix II

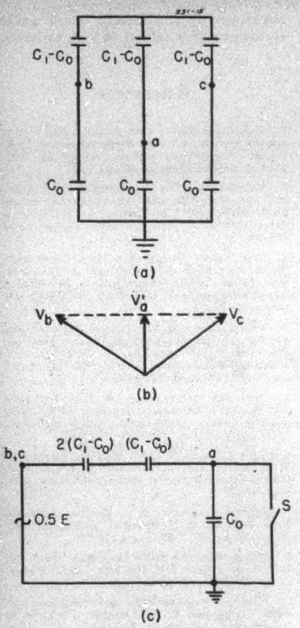


Figure 15. Circuits used in derivation of equations of appendix III

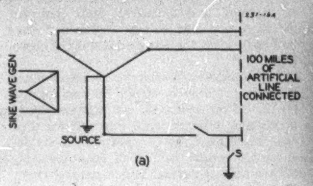
be interpreted for nine-cycle clearing when the inertia constant is increased from 3.0 to $(1.5)^2\times3.0=6.75$ by increasing the de-energization times by a factor of 1.5.

Appendix III. Self-Clearing Characteristics of Arcs to Ground With Single-Phase Switching

H. A. PETERSON*

MEMBER AIEE

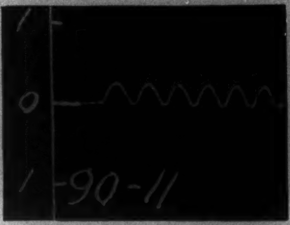
In the case of single-phase switching, the capacity coupling from the sound phase or phases to the stricken phase or phases tends to maintain an arc to ground once it is established. This is illustrated in Figure 15 of this appendix. Figure 15a shows the capacitance circuit involved. If it is assumed that phase *a* has been de-energized, the effective voltages in the circuit are V_b and V_c of Figure 15b. These can be resolved into components approximately as shown. The components V_b-V_a' and V_c-V_a' will have no effect on the voltage or current of conductor "*a*", since they are equal and opposite. The resultant effective component is $V_a'=0.5V_b=0.5V_c$. This is effective in the single-phase equivalent circuit shown in Figure 15c where conductor "*a*" retains its identity while conductors "*b*" and "*c*" are shown as a common point.



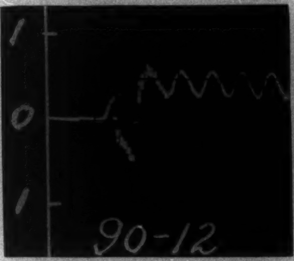
(a) Source reactance: $x_0=x_1=x_2=25$ ohms
Line:
$r_1=0.15$ ohm per mile
$r_0=0.36$ ohm per mile
$x_1=0.80$ ohm per mile
$x_0=3.2$ ohms per mile
$C_1=1.6C_0$
$C_0=0.01$ µf per mile

Figure 16. Transient analyzer circuit and oscillograms of line-to-ground fault clearing phenomena

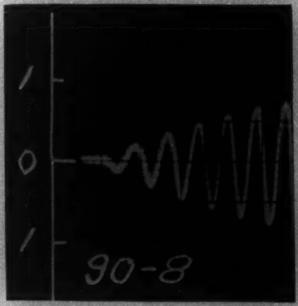
Refer to appendix III



(b) 90-11—Recovery voltage for single-phase switching of line-to-ground fault with no restriking. Interruption at normal current zero



90-12—Recovery voltage for single-phase switching of line-to-ground fault with restriking. (Two restrikes)



90-8—Recovery voltage for line-to-ground fault with ground-fault neutralizer

With switch S closed, a current flows through it to ground corresponding to the arc current which must be extinguished before conductor "*a*" can be normally energized for carrying power. The current flowing through S is

$$I_s=\frac{0.5V_b}{\dfrac{1}{\omega(C_1-C_0)}+\dfrac{1}{2\omega(C_1-C_0)}}$$

$$=\frac{0.5V_b}{\dfrac{3}{2\omega(C_1-C_0)}}=\frac{\omega(C_1-C_0)V_b}{3}$$

In terms of normal charging current for the section of line being switched out dividing by ωC, this becomes

$$\frac{I_s}{I_{nc}}=\frac{\omega(C_1-C_0)V_b}{3\omega C_1}=\frac{(C_1-C_0)V_b}{3C_1}$$

where I_{nc} is normal positive phase sequence line charging current. In a typical overhead line, $C_0=0.6C_1$ is representative. Therefore,

$$\frac{I_s}{I_{nc}}=\frac{(C_1-0.6C_1)V_b}{3C_1}=\frac{0.4V_b}{3}=0.133V_b$$

or the current to be extinguished is 13.3 per cent of the normal line-charging current of the line section being switched with normal line-to-ground voltage on the other phases. Thus the arc current is proportional to line length switched out and system voltage.

The recovery voltage V_a appearing across the switch S following the extinction of the arc current is

$$V_a=\frac{0.5/\omega C_0V_b}{\dfrac{3}{2\omega(C_1-C_0)}+\dfrac{1}{\omega C_0}}$$

$$=\frac{0.5/C_0V_b}{\dfrac{3C_0+2(C_1-C_0)}{2C_0(C_1-C_0)}}=\frac{(C_1-C_0)V_b}{C_0+2C_1}$$

Again, assuming $C_0=0.6C_1$

$$V_a=\frac{(C_1-0.6C_1)V_b}{0.6C_1+2C_1}=\frac{0.4V_b}{2.6}=0.154V_b$$

or the voltage on "*a*" due to capacitance coupling following the extinction of the arc is 15.4 per cent of normal line-to-neutral voltage.

Similarly, from Figure 15a, it can be shown that with two phases "*b*" and "*c*" switched out, each having an arc to ground, the current magnitude in each arc is the same as for the single-phase case just considered. Furthermore, the voltage on the conductor first to clear is also the same in magnitude as the voltage for the single-phase case. However, the arc current for the last phase to clear of the two stricken phases will be increased when the first arc goes out. If conductor "*b*" clears first

---

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$$\frac{I_e}{I_{ne}}=\frac{C_1-C_0}{2C_1+C_0}V_a$$

which, if $C_0=0.6C_1$, becomes

$$\frac{I_e}{I_{ne}}=\frac{(C_1-0.6C_1)V_a}{2C_1+0.6C_1}=\frac{0.4V_a}{2.6}=0.154V_a$$

or the arc current has increased from 13.3 per cent to 15.4 per cent of normal line-charging current in the section being switched out. The voltage on this phase when it has cleared will become

$$V_c=\frac{(C_1-C_0)V_a}{C_1+2C_0}=V_b$$

as the voltage magnitudes are equal on both conductors after both are cleared. If $C_0=0.6C_1$

$$V_c=V_b=\frac{(1-0.6)V_a}{1+1.2}=\frac{0.4V_a}{2.2}=0.182V_a$$

or the voltage on the conductors when both are cleared is 18.2 per cent of normal line-to-neutral voltage as compared to 15.4 per cent for either the single-phase case or the first conductor to clear for the two-phase case. Thus, it might be repeated that deionization times will be longer for double-line-to-ground than for single-line-to-ground faults.

In the extinction of these arcs, the *transient* recovery characteristic is also of importance. In order to study this phase of the problem, the transient analyzer was used. The system shown in Figure 16 was set up in miniature with the line open at the receiving end and only phase "*a*" open at both ends. By means of synchronous switching devices,[10] the arc current through switch *S* was interrupted at current zero. Oscillogram 90-11 shows the nature of this recovery voltage. It is important to note that the a-c component is completely offset, so that the instantaneous voltage to be cleared in each case is actually twice the sustained fundamental frequency component, or approximately from 30 to 35 per cent of normal line-to-neutral crest voltage. Furthermore, this voltage is reached one-half cycle after the assumed instant of clearing.

It is of interest to observe the effect of arc restriking. This is shown in oscillogram 90-12. Two restrikes, at approximately maximum voltage, were imposed for this case which gave rise to a voltage on the de-energized conductor "*a*" of approximately 65 per cent of normal line-to-neutral crest. These restrikes were controlled so as to give the maximum possible voltage per restrike. In the actual case, two restrikes would be extremely unlikely to give voltages this high. However, it is very likely that a good many more half cycles of arc current will flow before final interruption takes place, thus making possible a far greater number of restrikes.

A comparison of this voltage recovery characteristic and that of a ground-fault neutralizer system following the self-extinction of an arc to ground is enlightening. For the same system shown in Figure 16, but grounded through a neutralizer, the recovery characteristic is as shown in oscillogram 90-8. This illustrates the relatively slow "drift" of recovery back to normal restored voltage, taking three or four cycles to get up to 50 per cent of normal line-to-neutral voltage. Furthermore, arc restriking cannot increase this rate of voltage recovery, because each restrike simply causes the entire process to be started over again.

The preceding discussion has dealt with the dissimilarities of these two recovery characteristics. If only the first cycle following arc extinction without restriking is considered, there are also important similarities. In this interval, the recovered voltage versus time is of the same order of magnitude for both cases. Since this is likely to be the most significant interval of time during the extinction period, it is likely that arc extinction times may be expected to be somewhat similar for both cases. This time varies over wide limits, depending upon several factors, such as tower-footing resistance, atmospheric conditions, system voltage, system losses, and so on. Even with all of these factors fixed, there is an inherent randomness in arc behavior which makes arc clearing time subject to wide variation.

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Supplement to Electrical Engineering—Transactions Section

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Impulse and 60-Cycle Characteristics of Driven Grounds—II

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THIS paper extends the contribution on grounds of the first paper[1] in the following directions: experimentally, theoretically, and from the application standpoint.

The experimental data are from tests on twelve additional grounds driven in the field adjacent to the Sharon high-voltage laboratory. Seven of the grounds are in clay soil in a location which is naturally moist; these Meggered from 10 to 40 ohms. Two of the grounds are in naturally dry and gravelly soil, and one is in sand; the Megger resistance for these is from 60 to 220 ohms. The remaining two are in soil which consists of a mixture of clay and stones and Meggered from 25 to 190 ohms. The depth of the grounds ranges from 8 to 30 feet. Impulse and 60-cycle data are presented both for the single grounds and for parallel combinations. The impulse data extend up to 15,000 amperes and cover both positive and negative waves as well as other conditions. Tables, curves, and typical cathode-ray oscillograms present the more pertinent findings.

Analysis of the data shows that the decrease in resistance of driven grounds with increasing impulse current can be considered as the result of an increase in the effective radius and length of the rod. The agreement between experimental curves and curves calculated in this manner establishes the fact that the soil surrounding the rod breaks down at a critical voltage gradient. The two important factors associated with the soil are the resistivity and the breakdown gradient. In addition to providing a basic explanation of the physical processes involved, the analysis indicates that the impulse characteristic curve of a ground may be extrapolated with reasonable assurance to higher currents than the experimental data alone would permit. Furthermore, the methods established may be extended to study the relative characteristics of other types of grounds and arrangements, such as parallel grounds, the footing of tower structures, and so on.

This paper considers briefly the effect of lead inductance in combination with the ground proper, and it points out the practical importance of the lead or tower structure drop particularly for the rapid current discharge associated with direct strokes. Other factors affecting the impulse discharge are the conditions in the earth related to the soil and geological structure.

Physical Characteristics of Grounds

The 12 additional grounds, designated as *E*, *F*, *G*, *H*, *I*, *J*, *K*, *L*, *M*, *N*, *O*, and *P*, were driven in the spring of 1940 with the assistance of the Pennsylvania Power Company. The physical characteristics of the grounds are summarized in Table I. A view of the location and arrangement is shown in Figures 1 and 2. The one-inch-diameter rods and the rod in sand were driven with a sledge hammer; all other grounds were driven with a gasoline-operated hammer. The deeper grounds, *L* and *P*, are made up of eight-foot rods which are joined with suitable coupling.

All of the grounds except *M* are in natural soil and are located within 80 feet of the high-voltage laboratory. The grounds are well removed from fences and adjacent conducting objects with the possible exception of grounds *N* and *O*. Other details of adjacent objects and the terrain are apparent from the figures. Ground *M* consists of a six-foot diameter, ten-foot-deep hole packed with ordinary sand. The rod is driven down in the center of the sand. The sand was left to settle fully six months before the first serie sof impulse tests was made. At the time of the second series of tests a year later, the soil had gained considerable consistency and hardness.

The physical nature of the soil for grounds *E*, *F*, *G*, *H*, *I*, *J*, and *P* became apparent from the examination of a six-foot-diameter, ten-foot-deep hole (Figure 3) which was dug at a location some 30 feet from the grounds. While details of the strata and formation of the soil can vary considerably over this distance, nevertheless, Figure 3 indicates what to expect at the grounds. About six inches or more at the top is vegetation soil, then follows to a depth of four feet a combination of clay and gravel with a sprinkling of sand. Below this is a strata of about a foot of compact gravel and rocks. From five feet down to ten feet, the bottom of the hole, the soil is a thick blue clay. Water seeped in the hole at a depth of three feet. While this may not be the exact depth of the ground-water level, it indicates evidently that the soil for these grounds is naturally moist. It is difficult to state with certainty how far down bedrock lies. Rod *P* was driven with the gasoline hammer, straight down as far as it could be driven. Apparently it struck bedrock at 30 feet. According to the Geological Survey of Pennsylvania, bedrock in this district consists of fine-grained sandstone with alternate layers of shale.

Grounds *K* and *L* are located on the flat part of the embankment which rises

Table I. Physical Characteristics of Grounds

Ground	Date Driven	Diameter of Rod and Material	Depth Driven (Feet)	Nature of Soil	Spacing Apart and Location*
E *F* *G* *H* *I* *J*	4-12-40	1-inch steel	10	Largely clay. Soil naturally moist. See Figure 3	Grounds *E*, *F*, *G*, *H*, *I*, and *J* spaced 10 feet 2 inches apart. Ground *P* offset west from group above. Grounds driven in field
P	4-11-40	$^5/_8$-inch steel	29	Rod *P* struck bedrock 29 feet 3 inches below surface	
K	4-11-40	$^5/_8$-inch steel	8	Gravelly with clay mixture. Soil dry	Grounds *K* and *L* 17 feet apart. Driven in embankment
L	4-11-40	$^5/_8$-inch steel	16		
N *O*	4-12-40	$^5/_8$-inch Copperweld	8	Partly stony. Soil fairly moist	Ground *N* located adjacent to wood pole and *O* 10 feet apart
M	5-1-40	$^5/_8$-inch Copperweld	8	Ordinary sand used in construction work	6-foot diameter 10-foot deep hole filled and packed with sand

*For details on location, spacing, and terrain, see Figures 1 and 2.

Paper 42-22, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 12, 1941; made available for printing December 10, 1941.

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The authors acknowledge the assistance in securing the test data of G. L. Candler, L. J. Besch, K. C. Kelley, and others of the Sharon high-voltage laboratory.

H.V. LABORATORY
OSCILLOGRAPH ROOM
8 FT EMBANKMENT
M
P
N
(FIELD)

Figure 1. General view of grounds

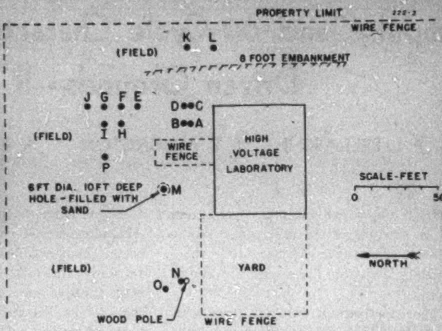


Figure 2. Arrangement of grounds

above the field where all of the other grounds are located. From *K* and *L* the land extends to the highway nearby, then up a hill rising beyond. A recent excavation of soil (October 1941) from the embankment near the grounds reveals that the top six feet are largely gravel with some clay, sand, and stones. Deeper down the soil becomes mostly clay with a mixture of sand. Due to the natural drain and the composition of the soil, grounds *K* and *L* are in naturally dry soil. This is particularly noticeable for ground *K* which in fact meggered the highest of all the grounds.

Grounds *N* and *O* are located in the flat terrain extending westward. Ground *N* is six inches from a distribution-line wood pole that was erected in 1934. Digging the hole for the pole at the time evidenced the presence of many large stones with clay, sand, and gravel. Ground *N* in particular and ground *O* to a lesser extent are embedded in soil of this nature. The terrain in which the twelve grounds are driven has a natural drain toward the Shenango River which lies a quarter mile westward at a level some 30 feet below.

The class and nature of the soil in which the twelve additional grounds are driven, as well as the physical characteristics of the grounds proper, represent a variety of typical conditions that should give rather wide scope to the investigation presented here.

Tests

The first series of impulse tests was completed late in the fall of 1940 before the ground frost started. Grounds *F*, *G*, *P*, *M*, and the parallel combination *F–G* were tested at this time. The results are summarized in Tables III and IV. For the grounds in clay (*F*, *G*, *P*) the wave is close to 20x50 microseconds, while for the ground in sand (*M*), it varies with increase in current from 8x125 to 25x65 microseconds. The currents for these tests cover a range of 400 to 15,500 amperes. Typical oscillograms are shown in Figure 5 (*AA* and *AV*) and Figure 6 (*AR*). Both positive and negative wave impulses were applied in the first series of tests.

The second series of impulse tests covers a period from August to September 1941. The results of these tests appear in Tables V, VI, VII, and VIII. The grounds tested are *F*, *H*, *I* and the parallel combination *F–G–H–I* all in clay soil; *M* in sand; and *K*, *L*, *N*, *O* in gravel and stones with a mixture of clay and sand. Some of these grounds were tested under the conditions of both fair weather and rain. In the preceding and other tests it was found that the polarity of the wave has little effect, and for this reason the second series of tests was made with negative waves, since this polarity appears to predominate in the lightning-stroke discharges to earth. In addition to the waves used in the first series, tests were

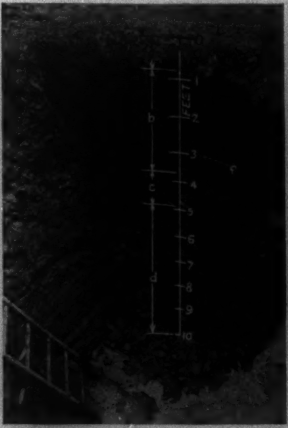


Figure 3. Section showing nature of soil for clay grounds

(a) Vegetation soil
(b) Combination of clay, sand, and gravel
(c) Strata of gravel and rock
(d) Thick blue clay
(*f*) Ground-water level

Table II. 60-Cycle Test and Calculated Values Compared

Experimental Values (Ohms) Single Grounds

Date of Test	E	F	G	H	I	J	K	L	N	O
7-22-40	28.0	23.0	21.6	21.8	22.3	26.9	133	49	54	24.4
12- 6-40	35.0	27.5	24.3	26.3	27.0	28.8				
3-20-41	40.0	27.5	23.0	27.0	30.0	32.0	218	65		
4-30-41	38.0	29.0	25.0	30.0	30.0	35.0	197	65		
6- 5-41	35.0	27.0	24.2	27.0	26.7	30.5	153	55		

Experimental and Calculated Values (Ohms) Grounds in Parallel

Date of Test	F & G		H & I		F–G–H–I		E–F–G–J		K & L		N & O	
	Exp.	Calc.	Exp.	Calc.	Exp.	Calc.	Exp.	Calc.	Exp.	Calc.	Exp.	Calc.
7-22-40	13.0	12.7	12.9	12.6	8.36	7.7	9.58	8.2	39	35	17.5	22
12- 6-40	14.5	14.8	15.0	15.2	8.75	9.1	8.75	9.5				
3-20-41	13.8	14.5	16.8	16.3	11.3	9.3	10.5	10.1	46	46		
4-30-41	15.5	15.4	16.0	17.1	11.0	9.9	12.0	10.5	50	46		
6- 5-41	15.0	14.6	15.0	15.3	9.3	9.1	9.3	9.6	42	39		
Average per cent error	2.5%		3.1%		8.5%		8.6%		8.6%		25%	

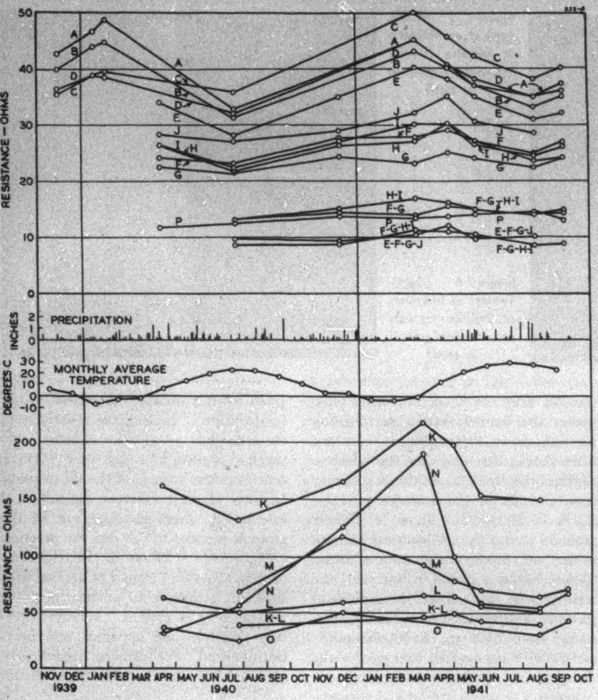


Figure 4. Seasonal variation of 60-cycle (Megger) resistance of grounds

also made with an 8x25-microsecond and a longer wave. The wave form and duration of the impulse for the tests on each ground are given in the tables. Typical oscillograms for these tests are shown in Figure 5 (*AL* and *AZ*) and Figure 6 (*CI* and *BE*).

Sixty-cycle measurements of the grounds have been recorded from time to time. Most of the readings were recorded with a Megger. These were taken through the assistance of the Pennsylvania Power Company in the conventional manner. In a few of the tests, the voltmeter-ammeter method was applied. This and previous investigations show that the Megger and the voltmeter-ammeter methods give practically the same resistance value. The two methods are, therefore, considered one and the same, and the data obtained from them are designated as 60-cycle resistance. In addition to the 60-cycle resistance measurements of single grounds and parallel combinations, the voltages developed from one ground to adjacent grounds were studied. The results of these tests are reported in Table IX. Similar tests made with impulse currents will be discussed in another section.

The method of test has been described in the first paper. Briefly, the impulse generator and the instruments are grounded to the common, low-resistance ground of the laboratory, which meggered for this investigation from 0.3 ohm to 0.9 ohm, and averaged 0.5 ohm to earth. The impulse voltage is recorded at the cathode-ray oscillograph through a voltage divider, and the current is recorded by means of a suitable shunt inserted in the grounded end of the impulse generator. Three values of impulse generator capacitance have been used, as stated in the tables. The impulses corresponding to each are nominally 20x50-, 8x25-, and 20x120-microsecond waves, the specific form depending on the resistance of the ground. The superimposed oscillations appearing on the front of the wave are due primarily to the combined effect of the stray capacitance and series inductance in the test circuit. For instance, *AR* of Figure 6 is an oscillogram with more than the usual amount of superimposed oscillations. These have no major effect on the ground proper but render difficult the analysis of the oscillograms below about three to five microseconds. It is an established principle that superimposed oscillations resulting from stray capacitance C_1 and series inductance L_s in the generator circuit may be damped out by a series resistance of $2\sqrt{L_s/C_1}$ or a load resistance of $^1/_2\sqrt{L_s/C_1}$. The series resistance is impractical for it materially limits the current delivered to the ground. A calculated resistance of 150 ohms was shunted across the surge generator with the improvement in the wave generated and applied as shown in oscillogram *CI* of Figure 6. Oscillograms *AR* and *CI* are directly comparable.

60-Cycle Characteristics

Sixty-cycle measurements for the 16 single grounds and five parallel combinations, taken over a period of two years, are plotted in Figure 4. The curves for the grounds in clay soil are grouped in the upper half of the chart; those for the grounds in sand, gravel, and stone are in the lower half. It is apparent that the seasonal variation of the resistance of grounds follows a cycle essentially inverse to the temperature cycle. While temperature appears to account largely for the seasonal change, rain precipitation, long dry spells, natural moisture content, composition of soil, and other factors can affect the resistance. For the grounds in clay, the variation from the annual mean resistance ranges from 10 to 20 per cent. The grounds in sand, gravel, and stone are subject to variations of 25 per cent and even more from the annual mean resistance. Since the annual variation of the average temperature of the top ten feet of soil would be in the order of 10 to 15 degrees centigrade, the temperature coefficient of the resistance of the soil is about two to three per cent per degree centigrade. These characteristics check the results reported by other investigators.[2,3,4] The deeper the ground is, the less will be the seasonal variation. For instance, *L* is twice as deep as *K* and has half the per cent variation in resistance. The seasonal change in the resistance of ground *P* which is 30 feet deep in clay soil is about eight per cent compared to 20 per cent for the nine-foot ground *A* also in clay. Ground *N*, embedded largely in stones, suffers very wide seasonal fluctuations in the 60-cycle resistance. The instability of this ground was further evidenced by the fact that while the 60-cycle measurements of all

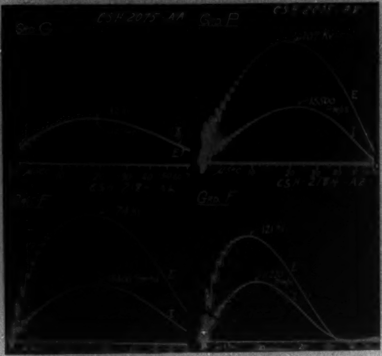


Figure 5 (left). Typical oscillograms of impulse currents and voltages (grounds in clay)

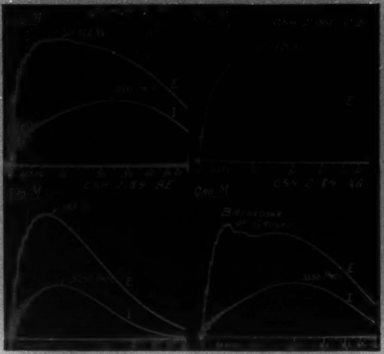


Figure 6 (right). Typical oscillograms of impulse currents and voltages (ground in sand)

the other grounds could be duplicated at a given time within the usual small experimental error, the check measurements of ground *N* would show wide variation.

The physical dimensions of the rod and the resistivity of the soil determine the 60-cycle resistance of a driven ground. The relationship is given in equation 1 of appendix I. While the physical dimensions are well defined, the resistivity of the soil may only be approximated, since it varies with the temperature and depends on the moisture content of the soil, the physical and chemical composition, as well as on other factors. Moreover, the soil may not be homogeneous even within the limited volume surrounding the rod, nor is the temperature constant from the surface down to the effective depth of the ground. All this is apparent from a study of the data for grounds, *E, F, G, H, I, J* in Figure 4. These grounds physically are identical and are in clay soil, driven within an area 10 by 30 feet. Over a period of one year, the resistivity of the soil for the individual grounds as determined from the equation ranges from 7,000 to 13,000 ohm-centimeters with an over-all average for the six grounds of 9,000-ohm-centimeters. Even when a suitable correction for the temperature is applied, the resistivity of the individual grounds for measurements taken at a given time may vary ±25 per cent from the average of the six grounds. In view of these inherent difficulties of calculation, direct measurement of the grounds remains to this day the practice.

Until more is known of the resistivity of soils, equation 1 cannot be applied with sufficient assurance to predetermine the resistance of a ground. However, certain principles are apparent and practically useful. For instance, according to equation 1 ground *P* (29 feet deep) should

Table III. Impulse Measurements of Grounds *F*, *G*, *F–G*, *P* (Clay)

Period of Tests 11-30-40 to 12-2-40

Driven Grounds	Wave Form (Microseconds)	Cathode-Ray Oscillogram 2075	Polarity	Measured Ground Crest Values: Kv	Measured Ground Crest Values: Amperes	Impulse Resistance (Ohms)	Ratio of Impulse to 60-Cycle Resistance	Comments*
F alone	Current wave 20x49 Voltage wave 18x50	*AB*	Pos.	31.5	1,545	20.4	0.74	Average Megger resistance of ground *F*, 27.5 ohms. Slight breakdown in ground at higher currents
		AC	Pos.	55.5	3,450	16.1	0.59	
		AD	Pos.	84.9	6,240	13.6	0.50	
		AE	Pos.	110.5	8,880	12.5	0.46	
		AF	Neg.	32.2	1,550	20.8	0.76	
		AG	Neg.	59.0	3,360	17.6	0.64	
		AH	Neg.	93.5	6,130	15.3	0.56	
		AI	Neg.	110.5	9,075	12.2	0.45	
G alone	Current wave 20x50 Voltage wave 19x51	*S*	Neg.	32.2	1,500	21.5	0.89	Average Megger resistance of ground *G*, 24.25 ohms. Major breakdown in ground at 6,000-ampere current and above
		T	Neg.	63.5	3,200	19.8	0.82	
		U	Neg.	91.8	5,550	16.5	0.68	
		V	Neg.	123.5	10,850	11.4	0.47	
		AK	Neg.	111.0	9,400	11.8	0.49	
		Y	Pos.	86.7	6,000	14.5	0.60	
		Z	Pos.	115.5	8,400	13.8	0.57	
		AA	Pos.	31.8	1,490	21.4	0.88	
F and *G* in parallel	Current wave 20x46 Voltage wave 19x47	*AL*	Pos.	115.0	8,130	14.2	0.59	Average Megger resistance of ground *F–G*, 14.5 ohms. Slight breakdown in ground at higher currents
		BC	Neg.	54.1	4,320	12.5	0.86	
		BD	Neg.	83.0	7,250	11.5	0.79	
		BE	Neg.	116	11,350	10.2	0.70	
P alone	Current wave 20x45 Voltage wave 20x45	*AS*	Neg.	26.5	2,480	10.7	0.78	Average Megger resistance of ground *P*, 13.75 ohms
		AT	Neg.	50	4,770	10.5	0.76	
		AU	Neg.	78.3	8,170	9.6	0.70	
		AV	Neg.	106.5	15,500	6.9	0.50	
		AW	Pos.	27.4	2,170	12.6	0.92	
		AX	Pos.	46.1	4,310	10.7	0.78	
		AY	Pos.	73.5	8,250	8.9	0.65	
		AZ	Pos.	94.5	11,700	8.1	0.59	
		BA	Pos.	95.8	11,700	8.2	0.60	

* In these tests the impulse generator consisted of three 100-kv, five-microfarad capacitor banks in series. Capacitance and inductance of test circuit were respectively 1.667 microfarads and approximately 250 microhenrys. No series resistance inserted in generator.

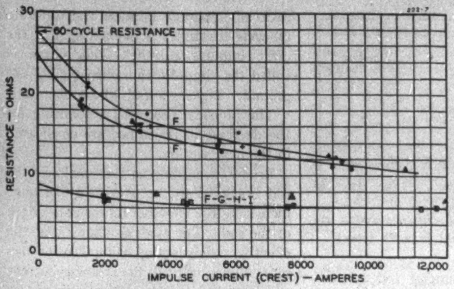


Figure 7. Variation of impulse resistance of clay grounds with polarity, wave shape, season and rain

Grounds in Clay

Test	Date	Polarity	Wave*	Weather
✚	Dec. '40	Pos.	20x50	Fair-cold
●	Dec. '40	Neg.	20x50	Fair-cold
⊙	Aug. '41	Neg.	18x45	Fair-warm
▽	Aug. '41	Neg.	18x45	Rain-warm
△	Aug. '41	Neg.	10x20	Rain-warm
⊡	Aug. '41	Neg.	18x40	Fair-warm

* Nominal value.

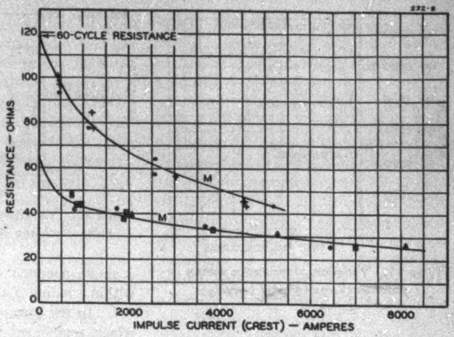


Figure 8. Variation of impulse resistance of sand ground with polarity, wave shape, season, and rain

Ground in Sand

Test	Date	Polarity	Wave*	Weather
✚	Dec. '40	Pos.	15x90	Fair-cold
●	Dec. '40	Neg.	15x90	Fair-cold
⊙	Aug. '41	Neg.	15x65	Fair-warm
△	Aug. '41	Neg.	8x25	Rain-warm
⊡	Sep. '41	Neg.	15x65	Fair-warm

* Nominal value.

measure 43 per cent of the resistance of a ten-foot ground driven in the same clay soil as *E*, *F*, *G*, *H*, *I*, *J*. Experimentally, ground *P* is 48 per cent of the average resistance of the six grounds. Equation 1 is also useful to determine the resistivity of different soils. Let us consider ground *K* in gravel, *M* in sand, and the grounds in clay. From the data we find that the average resistivities of the clay, sand, and gravel soils in this investigation are respectively 9,000, 20,800, and 41,800 ohm-centimeters or the resistivities are in the ratio of 1.0, 2.31, and 4.65.

For grounds in parallel, the 60-cycle resistance depends on the physical dimensions of the individual rods, the spacing and geometrical configuration of the rods, and the resistivity of the soil. The formulas and relationship for the parallel-ground arrangements in this investigation are discussed in appendix I. When the soil is fairly homogeneous, it is possible to determine the resistance of grounds combined in parallel, provided either the resistances of the single grounds are known experimentally, or the average resistivity of the soil has been established. Calculated and experimental data for the parallel grounds are compared in Table II. Grounds *E*, *F*, *G*, *H*, *I*, *J* in clay are in fairly homogeneous soil. The calculated resistance for the parallel pairs, *F–G* and *H–I*, is 57 per cent of the average single-ground resistance, for the four grounds *F–G–H–I* in a square arrangement and *E–F–G–J* in a row, it is respectively 35 and 33 per cent. The agreement between the calculated and experimental values for the two pairs, *F–G* and *H–I*, is within three per cent. For the two parallel groups, *F–G–H–I* and *E–F–G–J*, the agreement is within ten per cent. Part of the difference is accounted for by error in measurement, part is due to the assumptions in the calculation. Methods for determining the parallel resistance of two rods driven at different depths, as grounds *K* and *L*, have been developed. The calculated and experimental values for these two grounds which are in gravel lie within ten per cent of each other. A difference of 25 per cent is found for ground *N–O* in parallel. These two rods physically are identical. The large discrepancy is due likely to the instability of ground *N*, which is driven close to a wood pole, adjacent to a fence and possibly to other conducting objects in the soil, and is embedded in rocky soil. In summary, the resistance of parallel grounds can be calculated with good engineering approximation as a per cent of the resistance of the individual grounds, provided the conditions of the soil are fairly homogeneous, and no major extraneous effects are present.

The 60-cycle data in Table IX show that 85 per cent of the voltage applied to

Figure 9. Impulse characteristic for various soils and grounds

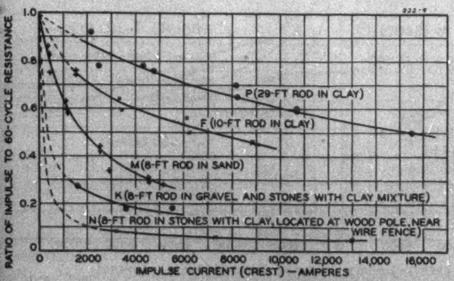
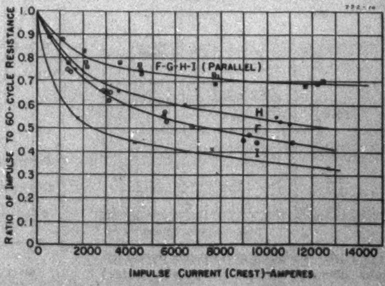


Figure 10. Impulse characteristic for single and parallel grounds



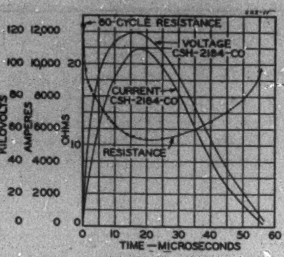


Figure 11. Variation of resistance during impulse discharge (ground *F*—clay)

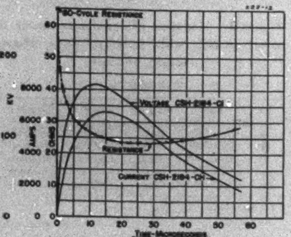


Figure 12. Variation of resistance during impulse discharge (ground *M*—sand)

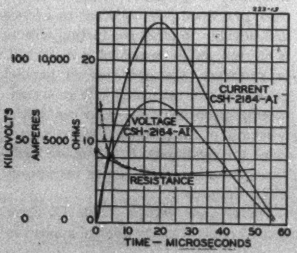


Figure 13. Variation of resistance during impulse discharge (parallel ground *F–G–H–I*—clay)

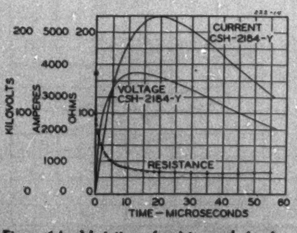


Figure 14. Variation of resistance during impulse discharge (ground *K*—gravel and clay)

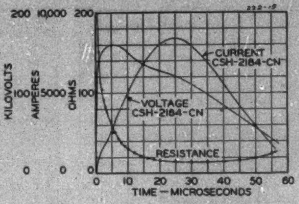


Figure 15. Variation of resistance during impulse discharge direct to earth (clay)

a single one-inch diameter rod appears within a radius of about ten feet from the rod. In the case of four rods in parallel which correspond to the four legs of a tower structure in the earth, 75 per cent of the voltage drop is absorbed within the first ten feet from the footings. Conditions of the soil can change this distribution of voltage and gradients, but on the whole, these data are in good agreement with the findings of other investigators.[4] In fairly homogeneous soil, it is apparent that the resistance of grounds is determined largely by the mass of earth within a few feet of the rods. In this confined region relatively high gradients may be developed, particularly adjacent to the rod. Analysis of the voltage distribution has been made and the results agree with the experimental data.

Impulse Characteristics

A. Variation of Resistance With Current

This and previous investigations indicate that when impulse currents are applied to driven grounds, the resistance decreases considerably with increasing current. The nature of this decrease can be shown by plotting the apparent resistance (crest voltage divided by crest current) against the crest current, as illustrated in Figures 7 and 8. To compare kinds of soil and types of grounds, the ratio of apparent impulse resistance to 60-cycle resistance is plotted against the crest impulse current. This curve is called the "impulse characteristic" of the ground: Figures 9 and 10 are examples.

The physical processes accounting for the decrease in resistance are considered in the next section.

Briefly, according to this analysis, the soil breaks down at a critical gradient, increasing the effective radius and length of the rod. The analysis shows that the effective radius is proportional to the current above critical breakdown current which corresponds to critical gradient at the rod surface.

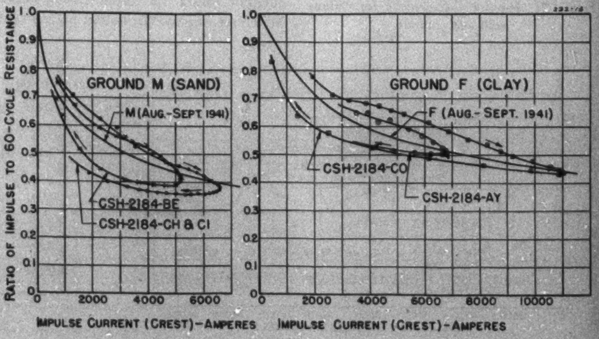
B. Factors Affecting Impulse Characteristic

The impulse resistance data of Tables III and V for ground *F* in clay and of Table VII for parallel ground *F–G–H–I* all in clay are plotted in Figure 7. Similarly the data of Tables IV and VI for ground *M* in sand are plotted in Figure 8. These figures show the effect on impulse resistance of the polarity of the wave, the wave form, the seasonal changes, and other influencing factors due to the weather such as rain.

Present data confirm that polarity has little effect on impulse resistance. Within the range of waves applied in this and the previous investigation (6x13 to 20x120), the wave of the impulse does not affect in an appreciable amount the impulse resistance characteristic.

On the other hand, a seasonal variation is clearly in evidence and is an important factor. For instance, the impulse resistances at 1,000 amperes for grounds *F* and *M* are subject to about the same per cent variation as the corresponding 60-cycle resistances. The seasonal effect is particularly marked at the

Figure 16. Variation of ratio during impulse current discharge compared with characteristic



low and medium currents but decreases at the higher currents. Tests on these grounds made within a few days of each other, both in fair weather and under or after sustained rain, indicate that rain does not affect the impulse resistance appreciably.

According to the analysis referred to previously, there are two properties of soil which determine in a large measure the impulse characteristic of a ground driven in it. These are the resistivity which determines the gradient for a given current density and the critical gradient at which the soil breaks down. The influence of soil on the impulse characteristic of grounds is illustrated in Figure 9. For comparison of results, curves for the similar grounds, *F*, *M*, *K*, and *N* are taken respectively from the data of Tables III, IV, and VIII. Considering critical gradient the same for two soils of different resistivity, the impulse characteristic of a rod driven in the high-resistivity soil will be moved to the left of the impulse characteristic of a similar rod in the low-resistivity soil by an amount approximately equal to the ratio of the lower resistivity to the higher resistivity. This relation is apparent from the curves in Figure 9 for grounds *F* in clay, *M* in sand, *K* in gravel and stones with clay, and *N* in stones with clay. As stated previously the 60-cycle soil resistivity for clay (*F*), sand (*M*), and gravel (*K*) are in the ratios of 1.0, 2.3, and 4.7. The characteristic curves for the three grounds are displaced to the left by an amount bearing an inverse relation to the corresponding resistivities of the soils. In short, grounds in very low-resistance soils approach the flat characteristic curve through unity which corresponds to a metallic conductor, while grounds in high-resistivity soil and of high resistance have a characteristic falling abruptly to low ratios. Grounds driven in high-resistivity soils such as *N*, *K*, and to a lesser extent *M*, may suffer in addition major breakdowns in the body of the soil, beginning at medium or even rather low currents, thus accentuating the characteristic still more. The voltage oscillogram of Figure 6-*AQ* illustrates a major breakdown in the body of the soil for ground *M* which occurred at a rather high current. This tendency for high-resistance grounds to break down at low currents has the effect of bringing closer together the resistance of low- and high-resistance grounds for high currents. For instance, ground *K* in gravel has a Megger value about six times as high as *F*,

Table IV. Impulse Measurements of Ground *M* (Sand)

Period of Tests 11-30-40 to 12-2-40

					Impulse Resistance (Ohms)						
	Wave Form				Resistance at						
			Measured Ground Crest Values					Ratio of Impulse to 60-Cycle Resistance			
Cathode-Ray Oscillogram 2075	Voltage (Microseconds)	Current (Microseconds)	Kv	Amperes	Crest Voltage (1)	Crest Current (2)	Apparent Resistance Crest V/I (3)	(1)	(2)	(3)	Comments*
Negative Polarity											
F	10x90	20x90	133	2,550	57.2	44.3	52.2	0.49	0.38	0.44	Average Megger resistance of ground *M*, 118 ohms. Slight breakdown at 4,500-ampere current and above
K	8x115	8x115	42.5	457	93	93	93	0.79	0.79	0.79	
L	8x85	25x90	125.5	2,550	64	45.8	49.2	0.54	0.39	0.42	
M	10x70	25x70	170	5,150	43.5	30.5	33.0	0.37	0.26	0.28	
R	9x100	16x100	82	1,100	77.4	73.1	74.6	0.66	0.62	0.63	
AM	8x125	8x125	41.9	425	98.5	98.5	98.5	0.83	0.83	0.83	
AR	10x60	25x65	165.5	4,500	45.4	28.6	36.9	0.38	0.24	0.31	
Positive polarity											
O	8x95	20x95	80.8	1,180	77.6	64.7	68.5	0.66	0.55	0.58	
P	8x75	25x85	122	3,000	56.5	35.3	40.7	0.48	0.30	0.34	
Q	8x65	25x70	155	4,570	43.2	30.2	34.0	0.37	0.26	0.29	
AN	8x125	8x125	40.4	400	101	101	101	0.86	0.86	0.86	
AO	8x125	8x125	41.7	433	96.7	96.7	96.7	0.82	0.82	0.82	
AP	8x100	25x100	81	1,150	84.5	64.4	70.5	0.72	0.54	0.60	
AQ	8x60	25x65	165.5	4,500	45.4	28.6	36.9	0.38	0.24	0.31	

*Same arrangement of impulse generator as in Table III. Inductance of test circuit approximately 200 microhenrys.

Table V. Impulse Measurements of Ground *F* (Clay)—Influence of Season, Rain, and Other Atmospheric Conditions; Effect of Wave

	Wave Form*		Measured Ground Crest Values						
Cathode-Ray Oscillogram 2184	Voltage (Microseconds)	Current (Microseconds)	Kv	Amperes	Impulse Resistance (Ohms)	Ratio of Impulse to 60-Cycle Resistance**	Date of Tests	Atmospheric Conditions	Comments***
A & *B*	14x50	17x50	24.4	1,310	18.6	0.75	8-22-41	Fair	Impulse generator capacitance $C_s = 1.67$ microfarads
C	16x50	18x50	47.7	3,120	15.3	0.62			
D	15x46	18x45	73.2	5,610	13.0	0.53			
E	16x43	19x42	99.4	9,000	11.0	0.45			
K	16x50	18x50	26.1	1,360	19.2	0.78	8-23-41	Fair	$C_s = 1.67$ microfarads
M	15x47	17x50	48.6	3,000	16.2	0.66			
N	16x46	18x45	78.0	5,540	14.1	0.57			
Q	16x44	20x42	103	9,570	10.8	0.44			
AJ	15x50	16x50	25.0	1,375	18.2	0.74	8-25-41	Rain night before and during the tests	$C_s = 1.67$ microfarads
AK	15x47	17x50	49.0	3,020	16.2	0.66			
AL	16x45	18x45	74.2	5,500	13.5	0.55			
AN	15x44	19x43	108	9,270	11.7	0.47			
AV	20x80	23x90	24.6	1,130	21.8	0.88	8-25-41	Rain	$C_s = 5$ microfarads
AW	20x80	23x90	12.2	553	22	0.89			
AX	8x22	9x22	48.2	2,950	16.8	0.66	8-26-41	Heavy rain night before tests	$C_s = 0.42$ microfarad
AY	8x22	10x22	86	6,750	12.7	0.51			
AZ	8x21	11x22	121	11,220	10.8	0.44			

*Negative polarity.

**Average Megger resistance of ground *F*, 24.7 ohms.

***Capacitance (C_s) of test circuit as indicated; inductance of test circuit approximately 250 microhenrys. No series resistance inserted in generator.

yet on a 5,000-ampere impulse discharge, it has an impulse resistance only twice that of *F*. All things being equal, a ground inherently low in 60-cycle resistance is desirable and preferred.

The influence of the effective length of rod in the earth on the impulse characteristic of grounds is illustrated in Figures 9 and 10. For comparison of results, the curves for grounds *P* and *F* in Figure 9 are from the data in Table III, and the curves for grounds *F*, *H*, and *I*, and *F–G–H–I* in Figure 10 are from Tables V and VII. Following the analysis previously mentioned, the impulse characteristic for a long rod, compared to that for a short rod of the same diameter and in the same soil, lies displaced along the current axis in proportion to the ratio of the lengths. As an example in Figure 9 the characteristic is shown for rod *P* which has 2.9 times the length of *F*. For the same ratios of impulse to 60-cycle resistance, the curve corresponds to currents from 2.2 to 3 times the values for *F*. The curve for the parallel ground *F–G–H–I*, Figure 10, is also compared with a single ground. Ground *F* which is representative of the four single grounds is used for this comparison. The total length of the four rods is four times the length of a single rod, and the characteristic of *F–G–H–I* would, therefore, be displaced to the right of *F* by a factor of four except for the mutual effect. For a ten-foot spacing the mutual effect is not great. For the same ratios the characteristic of parallel ground *F–G–H–I* lies displaced to currents that are two to four times the

Table VI. Impulse Measurements of Ground *M* (Sand)—Influence of Season, Rain, and Other Atmospheric Conditions; Effect of Wave

Cathode-Ray Oscillogram 2184	Wave Form*		Measured Ground Crest Values		Impulse Resistance (Ohms)			Ratio of Impulse to 60-Cycle Resistance** (3)	Date of Test	Atmospheric Conditions	Comments***
	Voltage (Microseconds)	Current (Microseconds)	Kv	Amperes	Resistance at Crest Voltage (1)	Resistance at Crest Current (2)	Apparent Resistance Crest V/I (3)				
R	8x80	12x80	32.2	795	41.7	41.4	41.7	0.64	8-23-41	Fair	Impulse generator capacitance, $C_s = 1.67$ microfarads. Slight breakdown in ground at higher currents
S	8x70	13x70	65.0	1,730	42.0	36.7	37.6	0.58			
T	10x60	15x70	116.5	3,650	34.3	30.6	31.9	0.49			
U	12x50	17x50	161.0	6,420	25.8	23.6	25.1	0.39			
AO	10x70	12x80	33.1	795	41.6	40.9	41.6	0.64	8-25-41	Rain night before and during the tests	$C_s = 1.67$ microfarads. Major breakdown in ground at higher currents
AP	6x50	20x60	68.5	2,340	41.8	25.9	29.2	0.45			
AQ	15x45	20x45	80.5	5,150	15.8	15.1	15.6	0.24			
AS	15x42	18x40	113	9,220	12.8	10.9	12.3	0.19			
AT	15x150	15x150	14.1	285	49.5	49.5	49.5	0.76	8-25-41	Rain	$C_s = 5.0$ microfarads
AU	12x150	12x150	28.3	570	49.6	49.6	49.6	0.77			
BD	5x25	7x25	76.4	2,010	38.9	37.5	38.0	0.59	8-26-41	Heavy rain night before tests	$C_s = 0.42$ microfarad
BE	6x23	9x23	145	5,250	30.8	26.2	27.6	0.43			
BF	6x20	9x21	200	8,100	26.7	23.2	24.7	0.38			
BY	10x70	12x80	36.2	840	43.1	42.4	43.1	0.67	9-10-41	Fair	$C_s = 1.67$ microfarads. Slight breakdown in ground at higher currents
BZ	10x70	10x60	35.4	740	47.8	47.8	47.8	0.74			
CA	10x60	12x60	72.7	1,915	37.9	37.6	37.9	0.58			
CB	10x70	12x80	41.0	935	43.8	41.5	43.8	0.68			
CC	12x65	12x70	73.6	1,960	39.5	34.9	37.6	0.58			
CD	10x55	15x55	122	3,840	32.6	28.2	31.7	0.49			
CE	12x45	18x47	172	7,000	26.1	23.9	24.6	0.38			

*Negative polarity.

**Average Megger resistance of ground *M*, 65 ohms.

***Capacitance (C_s) of test circuit as indicated; inductance of test circuit approximately 200 microhenrys. No series resistance inserted in generator.

Table VII. Impulse Measurements of Grounds *H*, *I*, *F–G–H–I* (Clay)

Period of Tests 8-22-41 to 9-9-41

Driven Grounds	Cathode-Ray Oscillogram 2184	Wave Form*		Measured Ground Crest Values		Impulse Resistance Ohms	Ratio of Impulse to 60-Cycle Resistance	Comments**
		Voltage (Microseconds)	Current (Microseconds)	Kv	Amperes			
F–G–H–I in parallel	*F–G*	18x40	20x43	14.5	2,160	6.70	0.76	Impulse generator capacitance $C_s = 1.67$ microfarads. Average Megger resistance of ground *F–G–H–I* in parallel, 8.8 ohms
	H	18x42	20x42	29.2	4,540	6.44	0.73	
	I	18x42	20x42	49.4	7,820	6.31	0.72	
	J	18x40	20x40	70.0	11,700	5.98	0.68	
F–G–H–I in parallel	*AD*	18x40	20x42	14.7	2,150	6.84	0.78	Impulse generator capacitance $C_s = 1.67$ microfarads. Average Megger resistance of ground *F–G–H–I* in parallel, 8.8 ohms
	AE	18x41	19x42	14.7	2,015	7.30	0.83	
	AF	18x41	20x42	30.0	4,450	6.74	0.77	
	AG	17x38	18x39	29.7	4,500	6.60	0.75	
	AH	18x41	20x41	46.9	7,730	6.07	0.69	
	AI	18x39	20x41	74.5	12,200	6.10	0.69	
F–G–H–I in parallel	*BA*	9x20	10x21	24.6	3,600	6.83	0.78	$C_s = 0.42$ microfarad
	BB	9x19	11x21	49.5	7,720	6.40	0.73	
	BC	9x19	10x20	76.4	12,450	6.13	0.70	
I Alone	*BO*	17x44	18x42	25.4	1,770	14.34	0.54	$C_s = 1.67$ microfarads. Average Megger resistance of ground *I*, 24.5 ohms
	BP	17x41	19x40	50.3	4,220	11.90	0.44	
	BQ	18x44	20x40	75.8	7,600	9.97	0.41	
	BR	20x42	21x39	102.5	12,700	8.07	0.33	
H alone	*BS*	16x43	18x43	55.5	3,500	15.6	0.66	$C_s = 1.67$ microfarads. Average Megger resistance of ground *H*, 23.5 ohms
	BT	16x42	17x38	90.5	6,480	14.0	0.60	
	BV–BU	18x43	19x41	133	11,000	12.1	0.52	
	BV	16x42	18x38	133	10,400	12.8	0.55	
	BW	16x43	19x40	132	10,600	12.5	0.53	
	BX	16x42	17x44	29.8	1,680	17.7	0.75	

*Negative polarity.

**Capacitance (C_s) of test circuit as indicated; inductance of test circuit approximately 250 microhenrys. No series resistance inserted in generator.

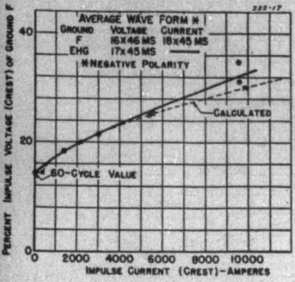


Figure 17. Voltage developed on *E–H–G* from impulse to ground *F*

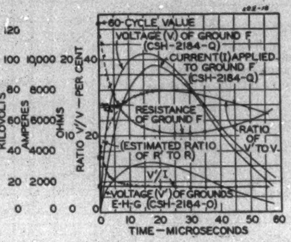


Figure 18. Variation of voltage on grounds *E–H–G* during impulse discharge to ground *F*

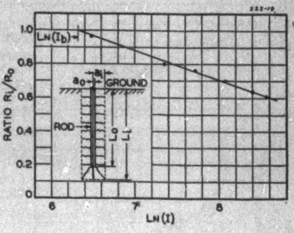


Figure 19. Method of determining the impulse characteristic of driven grounds

currents corresponding to the characteristic of *F*. There are other factors in regard to parallel grounds which will be discussed later.

The presence of metal fences, pipes, and other extraneous objects located adjacent to a driven ground can materially affect the impulse characteristics of the ground. This is the case for grounds *N* and *O*. The particular point to note here is that for the same soil, the ratio of impulse to 60-cycle resistance when referred to unit length of rod or to current density at the rod surface is a fairly typical form of curve which is a characteristic of the soil; although a departure from this characteristic may be expected even for grounds driven in presumably identical soil. This variation is due to the fact that the soil may not be altogether homogeneous and entirely the same throughout the mass of earth in which the grounds are driven, but can vary considerably from location to location as the curves for *H*, *F*, and *I* in Figure 10 clearly demonstrate. It is apparent from Figure 3 that a soil which for convenience may be classified as a kind can vary even for a single ground.

C. Analysis of Impulses

Valuable information concerning impulse characteristics can also be obtained by a direct analysis of the individual oscillograms. The method used is to plot the voltage and current from a given oscillogram to a uniform time scale and plot the resistance throughout. Typical oscillograms for grounds in clay, sand, and gravel are plotted and examined in Figures 11, 12, 13, 14, 15, and 18. Some of these oscillograms are practically free of superimposed oscillations on the front, as discussed in a preceding section with reference to oscillogram *CI* of Figure 6. In others these extraneous effects are averaged out.

In Figures 11 and 18, the resistance of ground *F* in clay is analyzed during the discharge for impulses close to 10,000 amperes crest. These results are in substantial agreement with the previous studies[1] on single grounds in clay soil. At the start of the impulse, the resistance appears to be the same as the 60-cycle value, dropping with the rising current on the front and reaching a minimum near and just beyond the crest current. As the current recedes beyond the crest, the resistance rises, apparently returning or directed toward the initial value. With this high current, the resistance does not vary more than 15 per cent from the minimum value within ten microseconds of the crest current. For lower currents the variation is less. As illustrated in *AA* of figure 5, for moderate currents the resistance varies only a small per cent over a relatively wide range of the discharge before and after crest current. For currents in the order of 5,000 amperes for a 10-foot ground (*F*) and 15,000 amperes for a 29-foot ground (*P*), as shown in *AL* and *AV* of Figure 5, the variation over some ten microseconds before and after crest is still within ten per cent of the minimum

Table VIII. Impulse Measurements of Grounds *K*, *L*, *N*, and *O*

Gravel and Stones With Clay Mixture
Period of Tests 8-22-41 to 9-9-41

Driven Grounds	Cathode-Ray Oscillogram 2184	Wave Form* Voltage (Microseconds)	Wave Form* Current (Microseconds)	Measured Ground Crest Values Kv	Measured Ground Crest Values Amperes	Impulse Resistance Ohms	Ratio of Impulse to 60-Cycle Resistance	Comments**
K alone	*V*	9x100		12.2				Average Megger resistance of ground *K*, 140 ohms. Breakdown in ground at 1,500-ampere current and above
	W	12x70	22x80	63.2	1,590	39.7	0.27	
	X	15x65	20x65	98.0	3,600	27.2	0.18	
	Y	15x53	20x60	146	5,530	26.4	0.18	
	Y	14x58	20x60	150	5,530	27.1	0.18	
L alone	*Z*		18x70		915			Average Megger resistance of ground *L*, 52 ohms.
	AA	14x60	16x58	56.6	2,110	26.8	0.52	
	AB	15x55	17x52	97.2	4,070	23.9	0.46	
	AC	14x52	18x50	137	5,830	23.5	0.45	
O alone	*BG*	14x40	19x40	26.4	2,150	12.3	0.40	Average Megger resistance of ground *O*, 31 ohms. Breakdown in ground at 2,000-ampere current and above
	BH	15x39	19x40	42.5	5,050	8.42	0.27	
	BI	15x37	20x38	64.2	9,680	6.63	0.21	
	BJ	18x40	21x38	87.0	17,200	5.05	0.16	
N alone	*BK*	16x37	20x36	14.8	3,180	4.65	0.08	Average Megger resistance of ground *N*, 55 ohms. Breakdown in ground early in discharge
	BL	15x37	20x36	25.0	7,400	3.38	0.06	
	BM	16x35	20x35	35.4	13,000	2.72	0.05	
	BN	16x37	21x36	49.2	20,300	2.42	0.04	

*Negative polarity.

**Capacitance of test circuit, $C_s = 1.67$ microfarads. Inductance of test circuit approximately 250 microhenrys for grounds *K* and *L*, and approximately 150 microhenrys for grounds *O* and *N*. No series resistance inserted in generator.

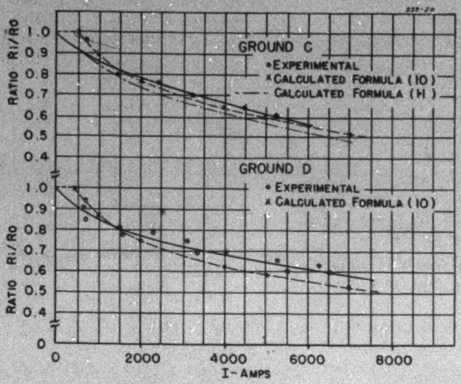


Figure 20. Impulse characteristic for grounds *C* and *D*

Calculated and experimental curves compared

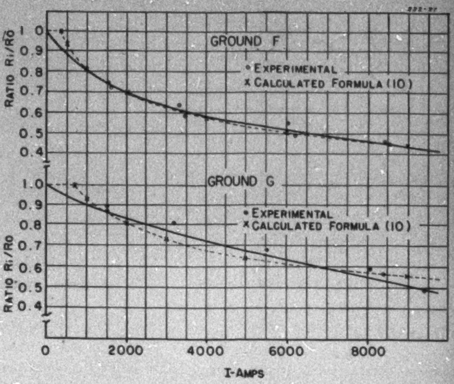


Figure 21. Impulse characteristic for grounds *F* and *G*

Calculated and experimental curves compared

value. In view of this small variation, the impulse resistance can be defined adequately as the ratio of crest voltage to crest current.

For the ground in sand (*M*) due to the higher resistivity of the soil, a greater variation is naturally encountered at the currents mentioned above. Figure 12 strikingly shows that the initial drop in the resistance from 65 ohms to 31 ohms at five microseconds follows rapidly the corresponding rise of the current from zero to two-third crest. As these oscillograms and the data in Tables IV and VI show, the resistance values of ground *M* corresponding to crest voltage and to crest current for these higher currents may differ as much as 30 per cent. Practical considerations have suggested to the authors that the ratio of crest voltage to crest current be accepted as the resistance, but to avoid possible misinterpretation, it is designated as the apparent resistance. As shown in Figure 14, even greater variation in the resistance is noted for ground *K* in gravel. An abrupt change from the initial value of 150 ohms to 60 ohms occurs in the first two microseconds. This rapid drop possibly is partly due to a major breakdown in the soil.

In Figures 11 and 18 the resistance curve of the clay ground *F* is indicated as a dotted line, dropping rapidly from the 60-cycle value. Even with good smooth oscillograms, a difficulty, in determining the true resistance initially and on the steep rising front during the first few microseconds, arises from the fact that the voltage (*E*) in addition to the resistance drop (*Ri*) includes an inductive drop (*Ldi/dt*) due to the rapid rising current and the presence of some inductance in the ground proper and the connections. For this reason, the curves determined from the ratio of voltage to current (*E/I*), which closely represent resistance down to three or four microseconds, are dotted in below these values. Estimating the inductance and correcting for the inductive drop, the authors find that the curve of the true resistance still points essentially toward the 60-cycle value initially at time zero. In Figure 13 is presented a similar analysis for parallel grounds *F–G–H–I* for an impulse in the order of 12,000 amperes crest. Up to this current the variation of the resistance for this ground, as is expected, naturally is relatively small. The true resistance during the discharge is shown by the full line. The high ratio of inductance to resistance inherent in this low-resistance ground thus shows emphatically the presence of the inductive effect (dotted line is *E/I*) in the early part of the discharge. For the higher-resistance grounds as *M*, the initial inductive drop is less prominent than for *F*, so that the ratio *E/I* in Figure 12, even below three or four microseconds, practically corresponds to the true resistance of ground *M*.

From the initial abrupt rise or "kick" of the voltage and the initial rate of rise of the current, scaled from a number of oscillograms, we estimate that the inductance as measured is about 15 microhenrys. Actual calculations of the inductance based on the dimensions of the driven rods and the lead connections of

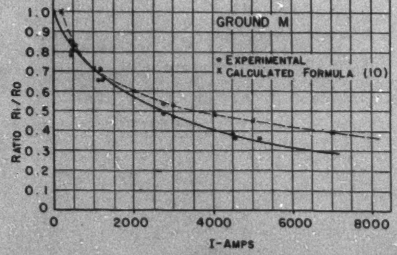


Figure 22 (left). Impulse characteristic for ground *M*

Calculated and experimental curves compared

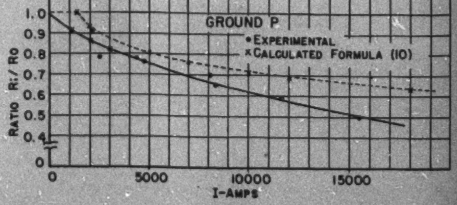


Figure 23. Impulse characteristic for ground *P*

Calculated and experimental curves compared

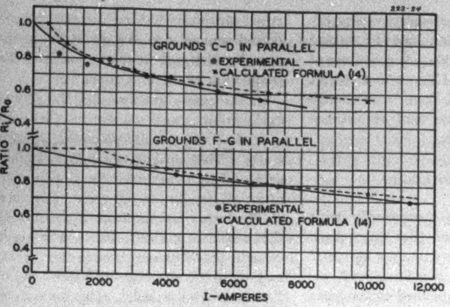


Figure 24. Impulse characteristic for parallel grounds C–D and F–G

Calculated and experimental curves compared

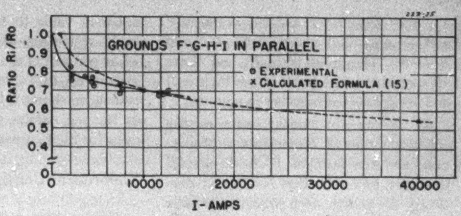


Figure 25. Impulse characteristic for parallel ground F–G–H–I

Calculated and experimental curves compared

the test confirm this value. About a third of the inductance as measured is in the rod proper, and even less in the large mass of the earth path of the discharge. The remainder, about ten microhenrys, is picked up in the leads, partly in the short segment of the discharge circuit proper from the top of the rod to the potential lead, and a greater part induced from the discharge circuit in the potential lead. The potential lead extends some 30 feet to the voltage divider, which is located adjacent to the oscillograph room.

The variation of the ratio of impulse to 60-cycle resistance during discharge for grounds *F* and *M* and currents from 5,000 to 11,000 amperes (crest) is shown in Figure 16. The ratio is plotted against current and is compared to the impulse characteristic (ratio for crest current) for each of the two grounds. The variation during discharge follows fairly close to the characteristic, intersecting it near crest current, and then returns below the characteristic, describing a considerable loop much in the same manner as found in the first investigation.[1] Since an inductive drop is present for the rapidly increasing part of the current, the true resistance curves would be even closer for the downward part than indicated. The analysis of these data and the previous curves establish that the variation of the true resistance rapidly follows the current with no appreciable time lag for wave fronts as fast as some two or three microseconds, the limit that the data can be interpreted. On the basis that the lowering of the resistance is a breakdown or sparking phenomenon consisting of small arcs, theoretically it stands to reason that little or no time lag in the change of the resistance would be expected for wave fronts longer than one or two microseconds. The exact fundamental mechanism which determines the lowering of the resistance is not well defined and established, and it is quite possible that the true resistance for a very rapid rise of current to crest in one microsecond would result in a time lag in the decrease of the resistance from high to low value.

It is apparent from the oscillograms and the figures that the effect of the high resistance of the ground in the initial stage of the discharge is to displace the voltage wave from the current and give it a more abrupt and steeper front. This is clearly shown by a comparison of Figures 11, 12, 14, and 15 in their sequence order. The higher the resistance of the ground the more accentuated is the effect. The inductance of the ground and lead connection also adds to displace the front of the voltage even further forward. Therefore, the rate of rise of the voltage for the same current is proportionately greater for grounds in high-resistivity soils and having high inductance associated with them. Due to the sustained ionization process, the low value of the resistance at and beyond crest current is sustained for a good part of the duration. The subsequent recovery of the resistance does not seem to have particular practical significance.

The question may be raised as to the resistance of a lightning-stroke discharge direct to earth. Figure 15 records a typical discharge of 8,300 amperes crest to the open field (clay soil) adjacent to the laboratory. All driven grounds and conducting objects are well removed from the spot hit. A small part of the grass at the earth's surface was scooped out and thrown up by the discharge. The resistance initially is very high, but as the discharge penetrates or pierces the earth, it drops to 60 ohms in five microseconds and still further to a minimum of 18 ohms at crest current. The stroke discharge, therefore, "drives" its own ground, for the resistance attains a value not much greater than that of a ten-foot rod driven in soil similar to that struck. On the other hand, the initial high resistance develops a very steep-front voltage wave which reaches 90 per cent of its crest in about a microsecond: long before the current reaches its crest. The resistance continues to fall rapidly so that at crest current the voltage has dropped to 75 per cent of its crest value. There are a number of ramifications to this problem. These tests indicate that the resistance characteristic of the location hit by lightning may influence the rate of current discharge and, to some extent, limit the maximum current attained. The investigation of the influence of the earth on lightning should be continued.

D. Voltage Developed in Soil

The impulse characteristics of driven grounds are further revealed from a study of the voltages induced or developed on adjacent objects. Data on impulse tests of this kind are presented in Figures 17 and 18. In these tests impulse discharges from 1,000 to 10,000 amperes crest were

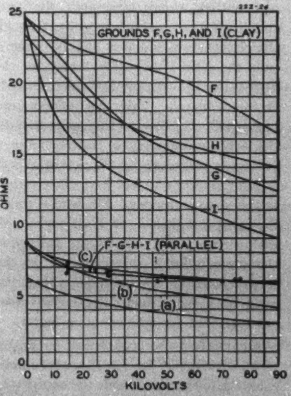


Figure 26. Method of determining impulse-resistance characteristic of parallel grounds from characteristics of component individual grounds

applied to ground F and the voltages V and V' measured respectively at F and E–H–G. The grounds E, H, and G are all spaced ten feet from F (see Figure 2) and are connected together in a group.

As the impulse current increases, the effective radius of F increases in proportion, and the ratio of V' to V should, therefore, increase; that is, a larger portion of the voltage drop should be outside the ten-foot radius. In Figure 17 the voltage developed on E–H–G is expressed as a per cent of the voltage at F so that the curve shows the variation of V'/V with the discharge current (crest). This ratio or coupling factor from a low current up to 10,000 amperes rises continuously, practically doubling in amount. These data further confirm that the effective size of the rod increases with the current.

In Figure 18 are reproduced oscillograms of the voltages and current and the variation of the coupling factor with the discharge. The voltage V' developed on E–H–G is of the same general form as the voltage V at F. The coupling factor V'/V naturally varies during the discharge. At crest current or slightly beyond, the effective radius and length of rod F reach a maximum; the coupling factor likewise rises to a maximum, and the resistance attains a minimum. As the current drops beyond crest, the sparking in the earth recedes, and ionization of the arcs subsides, so that the coupling factor falls off, and the resistance rises, both directed toward their 60-cycle values. From 20 microseconds (crest current) to 5 microseconds the coupling factor drops off. Still further down, two curves are shown, one dotted which represents the true coupling factor, and a heavy line through the circled dots which is merely numerically the ratio V'/V. The reason for the high ratio V'/V below five microseconds is found in the inductive effects and measurement. Due to the rapid current rise in the initial stage of the discharge, a voltage is picked up by the potential lead which runs some 30 feet from the grounds to the voltage divider. The lead is much the same whether V or V' is measured, with the result that an induced voltage of the same order is superimposed on the true voltage of the grounds. Thus the ratio V'/V below five microseconds does not represent the true coupling factor.

A curve has also been drawn on Figure 18 by dividing V' by I. This curve indicates the resistance outside the equipotential surface including E–H–G except for the first few microseconds which indicate inductive "kick." The resistance is practically constant, and, moreover, it is equal in value to the corresponding 60-cycle resistance, determined by taking 14.33 per cent of the 60-cycle resistance of ground F (see Table IX). This constant resistance shows that there is no change in the soil outside the ten-foot distance even for high currents.

Theoretical Considerations

This section deals theoretically with the physical processes that take place when ground rods carry impulse currents. The lowering of the resistance with increase of current is explained on the basis that at a critical current, a voltage gradient is attained at the rod surface causing the soil surrounding the rod to break down, thus increasing both the effective radius and length of the rod. The sketch insert in Figure 19 illustrates schematically the physical conditions which accompany a current above breakdown value. The irregular lines represent breakdown in the soil, and the dotted lines the enlarged effective dimensions of the rod. On this basis formulas and methods for determining the characteristic curves of grounds are developed. That breakdown of the soil may account for the lowering of the resistance had been proposed previously,[1,5,6] but the methods reported here enlarge on a quantitative approach which was set forth soon after the completion of the first paper.[7]

Table IX. 60-Cycle Voltage Distribution Tests

Date of Tests 7-22-40

Voltage Applied*		Voltage Recorded**	
Ground	Per Cent	Ground	Per Cent
J	100	E	3.3
		F	6.5
		G	11.9
		H	5.1
		I	8.1
		P	2.3
G	100	E	8.0
		F	15.5
		H	12.2
		I	15.8
		J	15.5
		P	3.0
F	100	E	13.5
		G	15.0
		H	14.5
		I	11.8
		J	7.5
		P	3.7
F–G–H–I in parallel	100	J	24.8
		E	26.6
		P	13.5
P	100	F–G–H–I in parallel	9.0
D–C in parallel	100	A–B in parallel	9.1
		Fence	0
A–B in parallel	100	Fence	0

*Voltage applied ranged from 80 to 200 volts. Current ranged from 5.5 to 18 amperes.

**Due to the voltmeter resistance (283 ohms) the actual voltage is a small amount greater than the per cent value indicated. No correction made.

On the basis of the foregoing theoretical explanation, there should be for each ground a current at which breakdown starts. This current we shall designate as I_b. It is determined by plotting the ratio of impulse to 60-cycle resistance R_i/R_o versus the natural logarithm of the impulse current lnI. The best straight line is drawn through the points, and where it crosses the ordinate $R_i/R_o = 1$, lnI_b is established. This procedure is illustrated for ground C in Figure 19. The value of I_b for grounds C and D, determined from the data in Table IV of the first paper, and for grounds F, G, P, and M, determined from the data in Tables III and IV of this paper, are tabulated in Table X. For all the grounds but M, R_i is obtained from the ratio of crest voltage to crest current. In the case of ground M (sand), R_i is obtained from the ratio of crest voltage to the current corresponding to crest voltage. In Table X are given also other essential data on these grounds; namely, the dimensions of each rod, the 60-cycle resistance, the soil resistivity ρ_o, and the critical gradient g_c at which the soil breaks down. The latter two quantities are determined respectively from equations 5 and 7 in appendix II.

Let us consider first the lowering of the impulse resistance as caused by the increase in the effective radius of the rod, disregarding the increase in length. On this basis we have equation 10 which relates the ratio R_i/R_o to the current I. From the data in Table X substituted in the formula, curves are calculated for the grounds C, D, F, G, M, and P. These are shown dotted in Figures 20, 21, 22, and 23, and may be compared directly with the experimental values. It is of interest to note that the effective radius of the ten-foot rods in clay increases to about ten inches at 10,000 amperes.

A refinement is introduced considering both the increase in radius and the increase in length as indicated by equations 11, 12, and 13 of appendix II. A curve for ground C has been calculated from these equations, substituting established values of a_i and estimated values of L_i. This curve is plotted in Figure 20. A rapid increase in the length at low currents, reaching an increase of about ten per cent of the length (L_o) at the higher currents, accounts for the lowering of the curve as shown.

The difference between the calculated curves and the experimental results is due, in part, to the various simplifying assumptions. We have assumed that the soil is homogeneous, that soil resistivity stays constant, and that breakdown in the soil is not accompanied by a voltage drop. Each of these assumptions is not fully valid, yet is sufficiently justified for an engineering approach. Allowing for the increase in rod length as discussed for ground *C*, and accounting for the voltage drop which accompanies breakdown, appear to bring the calculated curves for *C*, *D*, *F*, and *G* closer to the experimental data. The divergence for grounds *M* and *P* is apparently due to other factors which lower the characteristic at the higher currents a greater amount than the calculations indicate.

The validity of the breakdown process set forth above is further established by an analysis of the tests from which Figure 17 is obtained. Considering that the radius of *F* increases with current in the manner previously stated (equation 9, appendix II) and that the equipotential surfaces in the region included by *E–H–G* approach cylindrical form, the potential V' of *E–H–G* can be calculated as a function of current and is plotted in Figure 17 as the ratio V'/V. Other methods for calculating V' give essentially the same results. The agreement between experimental and calculated curves is apparent from the figure.

The procedure followed for a single rod can also be applied to parallel grounds. The methods are presented in appendix II. For two rods in parallel, the characteristic is given in equation 14, and for four rods arranged at the corners of a square, in equation 15. Calculated curves based on increase in radius only and experimental curves for parallel grounds *C–D* and *F–G* are compared in Figure 24. In these parallel grounds the two individual grounds in each arrangement are quite alike, and this similarity accounts for the close agreement between the calculated and experimental results. Allowance for the increase in the effective length of the rods with current would move the calculated curves in Figure 24 closer to the experimental curves in much the same manner as found for the single rods. The curves for the parallel ground *F–G–H–I* are plotted in Figure 25. The effect of the increase in length of the rods again is to move the calculated curve bodily down as described previously for other grounds.

The question arises whether the characteristic of a parallel ground may be predetermined from the characteristic curves of the individual grounds which comprise the parallel ground. In this connection ground *F–G–H–I* is examined in Figure 26, which shows resistance plotted against voltage. When an impulse is applied to grounds in parallel, the same voltage drop is present in each, and voltage is, therefore, used as the proper basis for comparison. In the figure the experimental characteristics are plotted and designated as *F*, *G*, *H*, *I*, and *F–G–H–I*. It is again to be noted that the single grounds differ. The characteristics of the four single grounds are first combined in parallel directly by the reciprocal relation as four completely independent grounds should be combined and are plotted in curve *a*. It is apparent that the mutual effect between the rods raises the resistance by an amount dependent on the mutual factor. For low current, the actual resistance for the four grounds in parallel is raised by a factor $0.35/0.25 = 1.4$ as found by equation 3, appendix I. Curve *b* is obtained by applying the factor 1.4 to curve *a*. Curve *b* follows the experimental curve at low currents but deviates at the higher currents, for the reason that the effective radius and length of the rods increase with current and the mutual factor becomes greater. The correct factor, which increases from 1.4 at low current to 1.65 at 10,000 amperes (62 kv), applied to curve *a*, thus gives curve *c*, which is the correct characteristic derived from the experimental curves of the four individual rods. Curve *c* compares favorably with the actual experimental characteristic of parallel ground *F–G–H–I*, and this comparison, therefore, shows that it is possible to predetermine the characteristic of a parallel ground from the experimental curves of the single grounds provided they are not too dissimilar.

Table X. Impulse Resistance Data for Ground Rods

Rod	Length Centimeters	Radius Centimeters	R_o Ohms	ρ_o Ohm-Centimeters	I_b Amperes	g_c Volts Per Centimeter
C	228	1.27	39.5	10,150	550	3,070
D	228	1.27	38.5	9,950	495	2,710
F	295	1.27	27.5	8,720	345	1,270
G	295	1.27	24.25	7,680	680	2,210
P	885	0.80	13.75	10,250	1,180	2,740
M	236	0.80	118	28,900	173	4,240

The foregoing analysis establishes that the decrease in resistance of driven grounds with increasing impulse current is due to the increase of effective radius and length. The agreement between calculated and experimental curves is close enough to establish that the soil surrounding a rod does have a critical voltage gradient at which it breaks down. This gradient is in the order of one to four kilovolts per centimeter. It follows from the equations, provided ρ_o and g_c of the soil are known, that the breakdown current I_b can be determined. Then from the dimensions of the rod or driven-ground system and the equations, the impulse characteristics of the grounds can be calculated. The need for further experimental study of the factors ρ_o and g_c is apparent.

In addition to providing a basic explanation of the physical processes involved, the results of this paper show that the calculated characteristic curve of a ground may be extrapolated with reasonable assurance to higher currents than the experimental data permit. Furthermore, the methods may be extended to determine the relative characteristics of grounds for various conductors and arrangements.

Effect of Discharge-Circuit Inductance and Conditions of Earth

Other important factors connected with the problem of grounds are the effect of the discharge-circuit or lead inductance and the conditions in the earth. A full discussion of these factors lies within the scope of the third paper proposed in the introduction. However, in the interest of a more complete survey of the problem they are dealt with here briefly.

A ground installation physically consists of a lead or tower structure which connects the grounded part of the electrical apparatus (protective device, apparatus tank, overhead ground wires, and so on) to the ground proper in the earth. The downlead or tower essentially can be considered an inductance, while the ground proper acts as a resistance in the manner discussed previously. The lead or tower inductance becomes particularly significant when steep-front currents of high magnitude, rising to crest in the order of a microsecond, are discharged to earth. Analysis shows that for high rates of rise of current, the voltage developed in a 25- to 50-foot lead or a 50- to 75-foot tower may well attain and exceed the ground-resistance drop. This lead or tower drop is of short duration and is practically over in one or two microseconds. Besides the lead or tower inductance, appreciable inductance may be present also in the ground proper, as is the case for grounds driven deep into the earth, or for a counterpoise spread over a wide area. Operating experience in the field [8] and the results of tests and co-ordination of station apparatus on steep-front impulses[9] corroborate the importance of inductance effects in protection.

For grounds of very high resistance, the capacitance of the rod or ground conductor to the mass of the earth is not entirely a negligible factor. The distribution of the electric charges in the earth preceding the current discharge through the ground is still another factor that requires study. Other factors related to the problem of grounds which require particular attention arise from the variation of the soil resistivity, the soil structure, the geology, and related questions.[3,10–14]

Summary

The 16 grounds tested are typical of those found in practice and represent a variety of soils and conditions. The resistance varies with soils, moisture, and soil conditions. A ten-foot ground Meggers 20 to 40 ohms in clay, 70 to 120 ohms in moist sand, and 100 to 200 ohms in a mixture of gravel and clay. The annual variation of this ground from the mean is 10 to 25 per cent, depending on the soil, and is less for deeper grounds.

The impulse resistance decreases with increasing current. It varies with season in much the same manner as 60-cycle resistance. Rain and weather conditions have little immediate effect. The impulse resistance is practically independent of polarity and of wave form within the range applied. The per cent decrease in resistance becomes greater with high-resistivity soils. During the discharge the resistance drops rapidly attaining minimum or impulse value near crest current and rises as the current decreases.

A method for determining the impulse resistance has been developed. At a critical gradient, the soil breaks down increasing the effective radius and length of the rod. Then from the equations in the appendix the impulse characteristic can be calculated. Curves calculated are in substantial agreement with the experimental results.

Various parallel combinations of single grounds were investigated. The resistance of parallel grounds, both 60-cycle and impulse, can be derived from the resistance of the component grounds.

The lead and ground inductance becomes particularly significant for rapid current discharges when the inductance drop may exceed the ground resistance drop.

Appendix I. Method of Calculating 60-Cycle Resistance of Single and Multiple Grounds

The formula used to calculate the resistance of single grounds is that given by H. B. Dwight.[15]

$$R_1=\frac{\rho}{2\pi L}\left[\!\!\left[\ln\left(\frac{4L}{a}\right)-1\right]\!\!\right] \qquad (1)$$

where

R_1 = resistance of single rod in ohms
ρ = soil resistivity in ohms per cubic centimeter
L = rod depth in centimeters
a = rod radius in centimeters

This formula may also be used to determine the soil resistivity from the dimensions and resistance of a ground. The following formula for two grounds in parallel is also given by Dwight.

$$R_2=\frac{\rho}{4\pi L}\left[\!\!\left[\ln\left(\frac{4L}{a}\right)-1+\ln\left(\frac{2L+\sqrt{S^2+4L^2}}{S}\right)+\frac{S}{2L}-\frac{\sqrt{S^2+4L^2}}{2L}\right]\!\!\right] \qquad (2)$$

where

R_2 = parallel resistance of two rods in ohms
S = distance between rods in centimeters

The following formula was derived by Dwight's method for four grounds of equal length arranged in a square

$$R_4=\frac{\rho}{8\pi L}\left[\!\!\left[\ln\left(\frac{4L}{a}\right)-1+2\ln\left(\frac{2L+\sqrt{S^2+4L^2}}{S}\right)+\frac{S}{L}-\frac{\sqrt{S^2+4L^2}}{L}+\ln\left(\frac{\sqrt{2}L+\sqrt{S^2+2L^2}}{S}\right)+\frac{S-\sqrt{S^2+2L^2}}{\sqrt{2}L}\right]\!\!\right] \qquad (3)$$

where

R_4 = parallel resistance of four rods in ohms
S = length of any side of the square in centimeters.

Formulas were also derived for four equally spaced rods in a straight line and for two rods of unequal length. The lack of complete symmetry of these arrangements makes the formulas rather complex, and for this reason they are omitted.

There are two methods commonly used for the calculation of grounds. Both methods employ the artifice of images by projecting an image upward a distance equal to the depth of the ground. The earth's surface thus bisects the rod, and all currents close to the surface flow parallel to it, satisfying the actual conditions. The earlier method, developed by O. S. Peters,[16] replaces the ground by an ellipsoid of revolution, whose minor axis is the diameter of the rod, and whose major axis is twice the depth of the rod. The capacitance of an isolated ellipsoid may be calculated directly, and from this, the resistance by the formula

$$R=\frac{\rho}{2\pi C} \qquad (4)$$

where C is the electrostatic capacitance of the ellipsoid. The more recent method, developed by H. B. Dwight, uses the rod in its exact shape and is based on the simplifying assumption that the charge distribution along the surface of the rod is uniform. Calculating for various points on the rod, the potential induced by this uniform charge, and dividing the total charge by the average induced voltage, Dwight finds the approximate capacitance of cylinder. This is converted to resistance by formula 4.

The two methods have their advantages: the former method lends itself readily to the calculation of equipotential surfaces and field distribution, while it is easier to calculate resistance for parallel grounds and noncylindrical rods by latter method. With typical values of L, a, and ρ, Peters' formula gives a resistance from five to ten per cent higher than Dwight's. This is expected since the surface area of the cylinder is greater than for the ellipsoid. It must be remembered, however, that both formulas are approximations, and that their accuracy decreases with decreasing ratios of L/a.

Appendix II. Method of Calculating Impulse Resistance of Single and Multiple Grounds

The basic formula used to calculate impulse resistance has the same form as that for 60-cycle resistance. Equation 1 of appendix I is adapted to the symbols shown in the insert of Figure 19, as follows

$$R_o=\frac{\rho_o}{2\pi L_o}\left[\!\!\left[\ln\left(\frac{4L_o}{a_o}\right)-1\right]\!\!\right] \qquad (5)$$

Considering the increase in the effective radius (a_i) of the rod resulting from breakdown of the soil at high current as discussed under "Theoretical Considerations" equation 5 can be rewritten

$$R_i=\frac{\rho_o}{2\pi L_o}\left[\!\!\left[\ln\left(\frac{4L_o}{a_i}\right)-1\right]\!\!\right] \qquad (6)$$

The effective radius of the rod is determined as follows. Since the voltage gradient along the surface of the rod is the product of current density and soil resistivity (ρ_o), the critical value of this gradient (g_c) at which the radius (a_o) of the rod starts to increase, is determined from the critical current (I_b)

$$g_c=\frac{I_b\rho_o}{2\pi a_o L_o} \qquad (7)$$

For current I above I_b, the gradient at the effective radius assumes critical value and is expressed by

$$g_c=\frac{I\rho_o}{2\pi a_i L_o} \qquad (8)$$

From 7 and 8

$$a_i=\frac{a_o I}{I_b} \qquad (9)$$

This value of a_t is substituted in equation 6 and the ratio of R_t to R_o can be written

$$\frac{R_t}{R_o}=\frac{\ln\left(\frac{4L_oI_b}{a_oI}\right)-1}{\ln\left(\frac{4L_o}{a_o}\right)-1} \tag{10}$$

Next let us consider the increase in length with current as shown in the sketch of Figure 19. Referring to equations 5 and 6 and allowing for the increase in length, we can express the ratio of R_t to R_o as

$$\frac{R_t}{R_o}=\frac{L_o\left[\left[\ln\left(\frac{4L_t}{a_t}\right)-1\right]\right]}{L_t\left[\left[\ln\left(\frac{4L_o}{a_o}\right)-1\right]\right]} \tag{11}$$

The relation of a_t to the current can be approximated by equation 9 or better determined from

$$\frac{I}{I_b}=\frac{a_tL_t}{a_oL_o} \tag{12}$$

The effective length L_t may be written as a function of the current

$$L_t=L_o+kI \tag{13}$$

where the factor k is constant only for limited ranges of current.

The impulse resistance for two rods in parallel can be obtained by substituting the proper values of a_t and L_t in equation 2 of appendix I. The ratio of R_t to R_o can then be obtained, and for reference purposes it is designated as equation 14. The impulse resistance for four arranged in a square can be obtained by a similar substitution in equation 3, and the ratio of R_t to R_o for this combination is designated as equation 15.

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The Application of Voltage Regulators to Aircraft Generators

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Synopsis: The use of high-capacity d-c generators on aircraft has required the development of regulators with rapid response, high accuracy, and with features for parallel operation of variable speed machines. The fundamental requirements of light weight, ability to withstand shock and vibration, and to give low radio interference were vital factors considered in the development. A device incorporating these features is described and analyzed. Results of tests on these equipments have shown that regulators with these features are vital to the operation of the electrical loads of modern planes, both the military and commercial types. The tests show that the requirements have been met with devices suited to high production methods.

THERE has been, and it seems safe to predict, will continue to be an increasing use of electrical devices in modern aircraft, both military and commercial. In order to supply the increased load, larger capacity generators than ever before utilized are being used. The generators are driven at variable speed from the main engines of the aircraft over a speed range of approximately two to one. Depending upon the size and use of the aircraft, an auxiliary power plant driving a constant-speed generator may be used in addition. A d-c system employing a 24-volt battery is in common use requiring a 28.5-volt bus voltage in the plane. The main generators must be controlled so as to maintain proper voltage for the battery and the various loads and, at the same time, insure close load division between the generators when operating in parallel. Close load division is important to allow utilization of the complete installed generating capacity.

The Importance of Generator Voltage Regulators

Voltage regulators play an extremely important part in making a system of this type practical. In fact, the system cannot be operated without suitable voltage regulators, since the variable-speed generators operating at low-flux densities at high speed are unstable to manual control. If the problem of parallel operation with close load division is accomplished by the same device, the importance of the voltage regulators in the system is immediately apparent.

Previous to the rapid increase in electrical loads on aircraft 12-volt d-c generators of small capacity had been used under the control of vibrating contact voltage regulators. Principal sources of trouble with the vibrating regulators were contact deterioration, radio interference, and failure to provide close load division of generators operating in parallel. Regulators of this type are inadequate for the larger capacity machines, on account of the increased power that must be handled in the generator field circuit which aggravates the troubles experienced on the small capacity machines. In this respect regulators for use in aircraft are closely following the history of regulators employed in stationary installations where the vibrating types have been largely displaced by other types. Direct-acting regulators have been successfully employed in stationary installation for sev-

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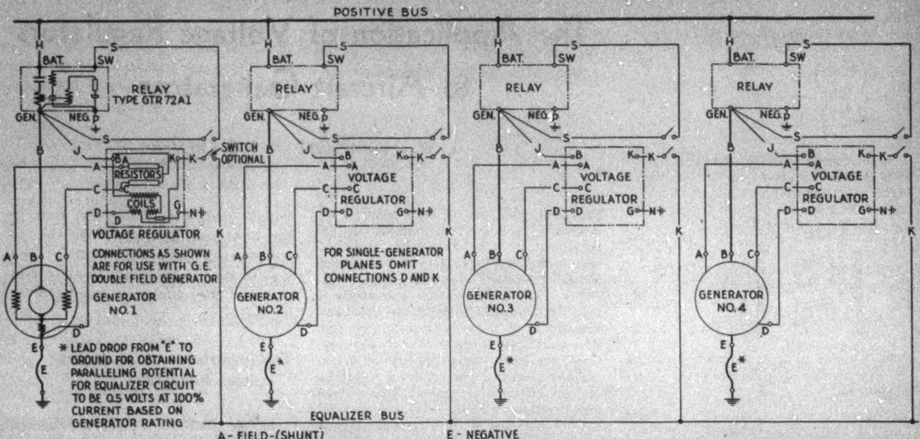
POSITIVE BUS
H
BAT.
SW
S
RELAY
TYPE GTR 72A1
GEN.
NEG.
J
B
A
C
D
K
SWITCH OPTIONAL
RESISTORS
COILS
G
N
VOLTAGE REGULATOR
CONNECTIONS AS SHOWN ARE FOR USE WITH G.E. DOUBLE FIELD GENERATOR
GENERATOR NO. 1
E
* LEAD DROP FROM "E" TO GROUND FOR OBTAINING PARALLELING POTENTIAL FOR EQUALIZER CIRCUIT TO BE 0.5 VOLTS AT 100% CURRENT BASED ON GENERATOR RATING
RELAY
VOLTAGE REGULATOR
FOR SINGLE-GENERATOR PLANES OMIT CONNECTIONS D AND K
GENERATOR NO. 2
GENERATOR NO. 3
GENERATOR NO. 4
E*
EQUALIZER BUS
A - FIELD-(SHUNT)
B - ARMATURE OR GEN. POS.
C - FIELD-(SHUNT-PARALLEL)
D - EQUALIZER CONNECTION
E - NEGATIVE
H - BATTERY OR BUS
K - EQUALIZER BUS
S - SWITCH-REMOTE CONTROL

Figure 1. Typical elementary connections for generators operating in parallel

eral years both on a-c and d-c generators. Their record has been very good from a standpoint of reliability, low maintenance, and freedom from excessive radio interference. Principles and means of providing for parallel operation have been worked out, and a great deal of experience gained on stationary installations. It therefore seemed that such regulators could be successfully used in aircraft installation, provided the special problems incident to their installation in aircraft could be solved.

Primary requirements for aircraft installation

1. A minimum of weight.
2. Ability to withstand shock and vibration.
3. Operation over a wide temperature range (from −55 to +65 degrees centigrade).
4. Ability to operate over an atmospheric pressure range from sea level to high altitudes.

Secondary requirements to meet present day requirements

1. Readily adaptable to generators having different field characteristics.
2. Suitable to quantity manufacture.
3. Simplicity of adjustment.

A regulator based upon principles found satisfactory in stationary installation has been designed to meet both the primary and secondary requirements listed above.

The Type of Regulator Applied

This regulator is of the direct-acting type and has a magnetic circuit energized by windings through a diluting resistor from the voltage of the generator being regulated. The force developed by the magnetic circuit by virtue of the applied voltage is balanced against a spring. At the required voltage setting the force of the armature overcomes the calibrating spring and operates in succession a multiplicity of contacts to insert small steps of resistance into the field circuit of the generator to maintain the voltage at which the regulator is balanced. It is necessary to so design the magnetic circuit that the mechanical forces are just balanced by the electromagnetic forces for all positions of the regulator armature at the same applied voltage.

By using a sufficiently large number of contacts the operation is made nonvibrating in nature, and the power handled per contact is kept below the critical arcing limit.

Figure 2. Plug-in-type regulator and relay for aircraft installation

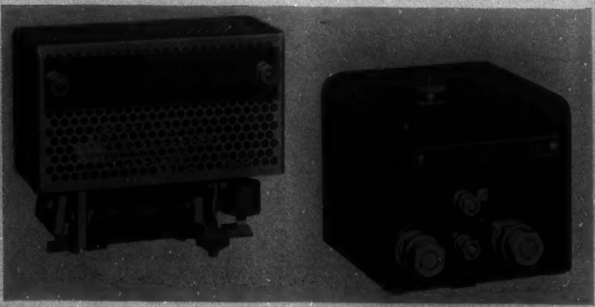

Figure 3. Regulator in separate case with *AN* corrector

Means for Accomplishing Parallel Operation

A second winding on the regulator is used to accomplish load equalization under parallel operation. The equalizing coils are connected as in Figure 1 and have been found very effective in obtaining equal load division of generators.

This method of equalization requires a small voltage drop (0.5 volt at rated load) in series with each generator. This drop does not cause a drooping voltage characteristic with load, as the voltage is regulated at a point beyond this drop. It does, however, cause a flow of current in the equalizing coils of the regulators when necessary to insure proper load equalization. In some instances the generators have an internal compensating winding which can be used to obtain the voltage drop for equalizing purposes. In order to obtain uniform drops and in order to insure interchangeability of generators of different manufacture, it has been the authors' recommendation that the potential drop required for equalizing purposes be made external to the generators, and in many instances the drop in the negative lead itself is utilized. In applications where the generator design is known, and an internal compensating winding is present, it is desirable to utilize this drop for equalizing purposes. In such instances potential dividers can be connected across these compensating fields to allow adjustment to compensate for differences in voltage drop.

Tests have been made with this scheme of equalization of load, and it has been demonstrated that close load division of generators in parallel can easily be obtained whether or not the speeds of the generators are equal.

When one or more generators in a group operating in parallel are disconnected, a drooping characteristic with load is obtained on the remaining units, unless the equalizing coil of the generator that is off the lines is opened. In some cases it is desirable to open the equalizer coil, and in other instances the drooping characteristic is desirable under these conditions, in order to allow the battery to share in supplying the higher load peaks.

In order to illustrate the practicability and desirability of the equalizing scheme shown in Figure 1, let us consider an example.

Suppose two regulators each having an accuracy of +1 per cent are to be operated in parallel and at a total load of twice the rating of one generator, the loads are to be divided within ten per cent of the rating of one generator.

Under the worst condition consider one regulator to be one per cent high and the other one per cent low on the basis of its potential winding alone. With the scheme using equalizing windings and 0.5-volt series drop at rated load, there will be at ten per cent difference in load 0.05 volt to cause current flow through the equalizing windings. The equalizing winding is designed so that this difference in voltage will cause a current to flow in the equalizing windings to change the ampere turns of the regulator one per cent of the total ampere turns, on one regulator in a direction to raise, and on the other in a direction to lower its voltage. These proportions have been found to be practical. There is a loss in the series resistors at rated load of approximately 1.75 per cent of the generator rating.

Now consider the same regulators connected without equalizing coils but with enough series resistance beyond the point of regulation to equalize the loads to the same degree. Assume that there is a difference in regulated voltage of two per cent with one regulator one per cent high and the other one per cent low. The series resistance between the regulated points must be such as to allow not over five per cent circulating current with two per cent voltage difference or 20 per cent resistance per generator circuit to obtain the same current equalization as with the scheme using equalizing coils. This is of course entirely impractical.

Other Considerations for Regulator Application in Aircraft

While regulators can be made to cover a range of field current requirements, it must be realized that the design of the generator field determines the power to be dissipated in the regulator. With fields of normal design the power dissipated for 200- and 275-ampere generators is about 65 watts. The aircraft manufacturer must provide sufficient ventilation for the dissipation of the heat.

While a regulator can stabilize generators that would be unstable on manual control, there are limits beyond which the regulator cannot stabilize the system. Generators with rising voltage characteristics as the load is increased are invariably unstable to a degree that the regulators cannot cope with on battery loads. This practically rules out the use of compound-wound variable-speed generators for aircraft application.

Regulators of the type described have been successfully applied on 120-volt systems. Also they have been applied to special purpose separately excited a-c generators by exciting the regulator coil through a dry-disc-type rectifier.

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The Magnetic-Drag Tachometer

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Synopsis: A discussion of a magnetic-drag-tachometer equipment designed principally for aircraft use. The principle of operation makes possible an indicator which may be read very accurately under extreme conditions of aircraft use and at the same time may be easily adjusted and maintained. Because of the extreme reading accuracy obtainable, low inherent temperature errors and good compensation are essential. A method of obtaining these features is presented.

MECHANICALLY operated aircraft tachometers include the centrifugal and chronometric types; prior to the development of the magnetic-drag tachometer, remote indicating electrical types included the a-c generator and voltmeter combination and the d-c generator and voltmeter combination.

The disadvantage of the mechanically operated tachometer rests in the excess weight caused by the necessity for a flexible cable between the engine and the indicator.

Generator and voltmeter tachometers lie in two general classifications, namely:

1. Voltage-sensitive devices.
2. Frequency-sensitive devices.

The voltage sensitive instruments may consist of a voltmeter, connected directly to a d-c generator or connected through a copper-oxide rectifier to an alternating generator. It is necessary to maintain a fixed lead resistance between the indicator and generator, and any change in generator output is immediately reflected in the accuracy of indication.

The problem of overcoming some of the objections of the voltage sensitive instruments was solved by the development of a frequency-sensitive tachometer whose indication is proportional only to the frequency of the generator output. This tachometer consists of a voltmeter connected to an a-c generator with a copper-oxide rectifier and a saturated core transformer. The transformer core is saturated throughout the operating range of the aircraft engine, and the output of the transformer is consequently directly proportional to engine speed. This development permitted the exchange of generators or indicators without calibration adjustment, eliminated changes in calibration due to changes in generator output produced by vibration or temperature changes, and permitted the installation of tachometer equipment without making it necessary to maintain a fixed lead resistance.

As the size and speed of aircraft increased, due to the availability of more powerful engines, it became necessary to control the engine speed more accurately in order to obtain the most economical engine operation. High altitude operation presented the problem of increased accuracy of engine speed indication throughout the temperature range from approximately −40 degrees centigrade to +50 degrees centigrade, requiring marked improvement in temperature compensation. Due to the more accurate indication required, it became necessary to obtain tachometer equipment with a greatly increased scale length to provide more accurate readability.

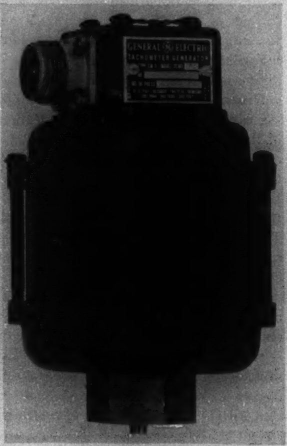

Figure 1. Tachometer generator

It is the object of this paper to indicate how the problems outlined in the preceding paragraph were solved by the development of a tachometer operating on the magnetic-drag principle and to present pertinent details regarding the operation of representative tachometers.

Consideration of the application of tachometer equipment to aircraft indicated that the wide variation between the temperatures of the indicator and the generator, in combination with the very severe vibration of the generator, made it absolutely essential that the indicator be entirely frequency responsive. This is necessary, due to the changes in generator-output voltage caused by vibration and to temperature changes which vary the resistance of the generator circuit.

The generator–voltmeter tachometer with saturated core transformer overcame problems due to the effect of vibration and temperature changes on the generator; however, as was true with other types of remote electrical indication tachometers, the maximum scale length obtainable was 270 degrees. In order to increase this scale length materially, a tachometer operating on the magnetic-drag principle was developed.

The Magnetic-Drag Tachometer

The magnetic-drag tachometer consists essentially of a three-phase a-c generator, mounted on the aircraft engine, and an indicator, consisting of a synchronous motor operating a magnetic-drag assembly.

The problem of changes in generator output voltage was eliminated by the adoption of the generator and synchronous motor combination, which constitutes a frequency-sensitive system for transmitting an indication of engine speed to the indicator with absolute accuracy.

For many installations it is desirable to obtain engine speed indication at two different stations in the aircraft. This system is ideal for this application, since there is no change in indication, due to the addition of a second indicator in parallel with the first; synchronous motor operation in each indicator is only dependent upon the availability of sufficient power to operate both motors.

Generator (Figure 1)

Primary factors of light weight and small size, plus trouble-free operation, were problems which had to be met in the generator design.

Light weight was accomplished by the use of aluminum alloy for the end shields.

Light weight and small size were obtained by making use of a permanent magnet rotor with high magnetic energy content. The physical dimensions of the rotor required a magnetic material having

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high coercive force, due to the short magnet length and in order to prevent demagnetization. Because of the extreme vibration encountered, it is necessary to have a material which is not readily affected by vibration. Alnico offered the answer to those requirements and is cast directly onto the rotor shaft.

In order to obtain high resistance to wear produced by vibration, stainless steel inserts were cast into the generator end shields.

As the aircraft-engine accessory drives often transmit to a considerable extent torsional oscillations originating at the crankshaft, the tachometer generator includes a long slender drive shaft carried within the hollow rotor shaft, having sufficient flexibility to prevent failure of the drive key as well as to accommodate small misalignments between generator and engine pad.

Two limiting conditions are necessary with regard to output of the generator:

1. With a single indicator connected to the generator, the voltage output at normal cruising speed must not exceed a certain limit in order to prevent excessive temperature rise in the indicator.

2. At the lowest operating speed, with two indicators connected to a single generator, the output voltage must exceed a certain minimum value, so that there will be sufficient power output available to start and operate the indicators at any temperature.

These requirements were met by taking advantage of the properties of the Alnico rotor which permitted shorter—hence lower resistance—turns, resulting in improved voltage regulation. Adjustment of voltage within the limits specified is obtained by means of an a-c knockdown coil after complete assembly of the generator; this adjustment also stabilizes the magnet to minimize effects of vibration.

Indicator

The indicator (Figure 2) as briefly described in a previous paragraph, includes a synchronous motor and a magnetic-drag assembly.

The requirements for a satisfactory motor include the ability to start and to run synchronously at any speed throughout the operating range, when two indicators are connected to a single generator, and yet not to overheat when connected singly to a generator. Since the generator output decreases with a decrease in engine speed, it is necessary that the indicator start with very low input at the lowest speeds. It is essential that the motor should meet these requirements with minimum weight and size and yet be simple and rugged for long life and ease of maintenance.

A unique motor assembly in conjunction with a conventional three-phase wound stator offered a solution to these requirements. The rotor assembly as shown in Figure 3 consists of a polarized rotor, a spring, and a hysteresis disk. The starting torque is supplied by the polarized rotor at low engine speeds, where the frequency is too low to produce appreciable torque in the hysteresis disk. As the generator frequency is increased by increasing engine speeds, a greater share of the torque is produced by the hysteresis disk, and the polarized rotor assists by bringing the motor into synchronism.

In order for the polarized rotor to perform the above functions, it was necessary to design a rotor with an extremely low moment of inertia and with high magnetic strength; a sintered oxide material was used for this application, resulting in a rotor having approximately four-tenths the weight of any other available magnetic material.

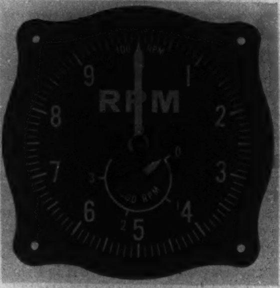

Figure 2. Magnetic-drag tachometer indicator

The hysteresis disk is produced from an alloy of copper, nickel, and iron, heat-treated to produce the desired magnetic characteristics.

It was found that there was very definitely an optimum spacing between the polarized rotor and the hysteresis disk to obtain the best starting characteristics throughout the entire speed range.

Bearings for the indicator motor presented a major problem for the following reasons:

Generator output decreases as the generator speed is lowered, resulting in a very low indicator input at engine idling speeds, thus requiring low friction in the bearings to permit proper starting.

Bearing lubricant gradually becomes more viscous as the temperature is decreased, resulting in increased bearing drag.

Since bearing life is dependent upon amount of lubrication it is desirable to supply the maximum quantity of lubricant possible.

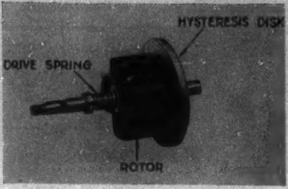

Figure 3. Indicator-rotor assembly

In order to properly protect the ball bearings until ready for installation in indicators, they are stored completely filled with lubricant; when needed for assembly, each bearing is thoroughly washed and cleaned to remove all traces of the lubricant, the bearing is then dried, and a definite quantity of inhibited black fish jaw oil is applied to the bearing. Repeated laboratory tests have indicated the superiority of this oil with regard to rate of evaporation, acidity, and change with continued evaporation. The quantity of lubrication applied is dependent upon minimum operating temperature to which the particular instrument is to be exposed.

The magnetic-drag assembly may properly be divided into two sections, the rotating magnet assembly and the armature assembly.

Since the rotating magnet assembly (Figure 4) is a part of the rotor assembly of the synchronous motor, it is necessary to obtain a magnetic assembly with as low a moment of inertia as possible, which will produce the required torque on the magnetic-drag disk. To meet these requirements five pairs of Alnico magnets are arranged as shown in Figure 4; this arrangement concentrates the flux near the outside edge of the magnetic-drag disk with minimum weight of magnetic material.

The armature assembly includes the shaft, springs, and pinion gear illustrated in Figure 5, in combination with a drag disk and pointer. The magnet assembly of Figure 4 and the flat drag disk permit ready inspection of the air gap, and by

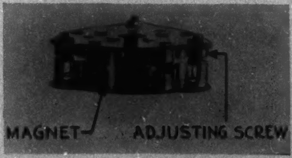

Figure 4. Rotating-magnet assembly

Table I. Comparison of Disk Materials

Material	Temperature Coefficient (Per Cent Per Degree Centigrade)	Conductivity	Density (Grams Per Cubic Centimeter)
Aluminum	0.34	60	2.7
Copper	0.385	100	8.9
Aluminum-manganese	0.040	12	2.8'
Copper-manganese	0.036	15	8.8

means of adjusting screws, the element may be raised or lowered to centralize the disk in the magnet gap.

The spring assembly must permit $3^1/_2$ revolutions of the pointer, must be rigid enough to withstand the effects of vibration, and must be designed to operate well below the elastic limit in order to prevent appreciable changes in indication when deflected for long periods of time.

These spring problems were solved by the unique spring arrangement illustrated in Figure 5. Here two springs are effectively connected in series by a spring arm joining the outer turns of the two springs, the inner turn of one spring is fastened to the shaft, and the inner turn of the second spring is fastened to an adjusting arm mounted on the element plate. By this method of construction $3^1/_2$ revolutions of the pointer result in only $1^3/_4$ revolutions of each spring, permitting shorter and consequently more rigid springs. Increased stiffness of the spring is also allowed, since the torque per degree deflection of each is double the torque of an equivalent single spring. With this arrangement no restraining plates are required, and the spring arm prevents any possible entanglement of the two springs.

The pinion actuates the short pointer illustrated in Figure 2 to indicate the number of revolutions of the long pointer.

The magnetic-drag disk is made from an aluminum-manganese alloy in order to obtain minimum weight with the requisite conductivity and temperature coefficient. It is essential to obtain a temperature coefficient within certain definite limits in order to obtain optimum accuracy of indication for rapidly changing temperatures. Data for several disk materials are indicated in Table I, where the advantage of the aluminum-manganese alloy is evident due to the combination of light weight and low temperature coefficient.

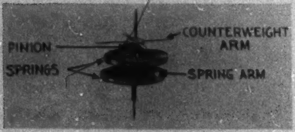

Figure 5. Spring Assembly

Temperature Compensation

In order to obtain the required accuracy throughout a temperature range from −35 to +45 degrees centigrade, it is necessary to provide compensation for the changes in indication due to these temperature changes. Changes are produced by

(*a*) Magnets.

(*b*) Disk.

(*c*) Springs.

(*d*) Other very small effects such as those caused by expansion and contraction.

As the temperature decreases, the magnet strength increases, tending to produce an increase in indication; the disk conductivity increases, also tending to increase the indication, and the torque of the springs increases, tending to produce a decrease in indication. The temperature-torque coefficient of the springs is approximately 0.04 per cent per degree centigrade; by proper heat treatment of the disk alloy, it is possible to obtain a similar coefficient, thus cancelling the errors produced by the disk and springs.

Both the thin disk and the springs are similarly exposed to the air inside the instrument case, so that they will quickly respond to rapid changes in temperature in order to eliminate transient errors.

The temperature error produced by the magnets is indicated by curve *A* in Figure 6 for an indicated speed of 3,000 rpm; compensation is accomplished by the addition of temperature-sensitive magnetic shunts placed in the magnetic assembly of Figure 4.

As the magnetic flux increases with decrease in temperature, it is necessary to shunt the extra flux directly across the poles of the magnet to maintain constant flux in the disk gap. These shunts are placed underneath the clips which fasten the magnets to the plates in Figure 4.

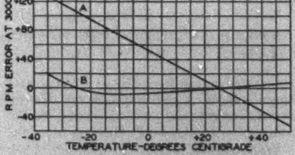

Figure 6. Temperature-compensation curves

A—Uncompensated indicator
B—Compensated indicator

The addition of a single type of temperature-sensitive material corrects the greater share of the fundamental change; this material is a nickel–iron alloy with a release point of approximately +90 degrees centigrade. This release point is the temperature at which the alloy becomes practically nonmagnetic.

Greater compensation is produced by this type of material above room temperature than below room temperature; to correct for this difference a copper–nickel alloy, with a release point at or slightly above room temperature, is added to produce the compensated curve *B* of Figure 6 for a representative instrument. Close contact between the temperature sensitive material and the magnets tends to maintain both materials at the same temperature regardless of the rate of change of temperature, thus eliminating transient temperature errors.

Calibration

A good tachometer should include:

1. Full-scale adjustment.
2. Zero adjustment.
3. Method of balancing element assembly.

Preliminary full-scale adjustment is obtained by the use of an a-c knockdown coil to reduce the air-gap flux produced by the rotating magnet assembly; by this method the magnet strength is reduced at least five per cent in order to stabilize the magnets and to bring the calibration within 0.1 per cent of the correct indication at full scale. After a calibration check has been completed throughout the scale range, final adjustment may be made by further application of the knockdown coil, or the indication may be increased by movement of any one or all of the three adjusting screws illustrated in Figure 4. These adjusting screws vary the air gap to increase or decrease the flux and permit ready and positive adjustment at any time.

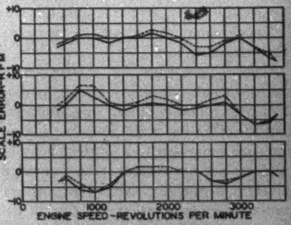

Figure 7. Calibration Curves

——— Engine speed increasing
– – – Engine speed decreasing

Synthetic or Equivalent Load Curves

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Synopsis: This paper describes short approximate methods for the solution of several types of intangible problems which require load-curve analyses. Laborious effort may be expended in compiling results from a group of curves without greater precision than is believed possible to obtain from the use of one or a few synthetic curves which combine the characteristics under investigation, and permit the determination of such items as peak loads, losses, and required capacities of equipment.

LOAD curves are familiar to engineers. The variety of precision instruments which have been developed to draw them automatically, and the ever increasing number of uses to which they are applied would require, for adequate description, an historical record dating from the days of the rocker-beam steam engine. Today they are so commonplace that no attempt will be made herein to classify them, or to review methods generally applied.

There are particular types of problems in which the characteristics of a number of similar curves, taken in the aggregate, are more helpful than those of any single curve. For example, in a study made recently, including the load curves of many large industrial and utility companies, it became evident that laborious effort may be expended in compiling results from a group of curves, such as daily curves for a yearly period, with scarcely greater precision in the end than is believed possible to obtain from the use of one, or a few synthetic curves which combine the characteristics under investigation.

One problem occurs frequently in electrical calculations; that involving losses, and consequently a squared function of a current or load curve. This can determine principally the capacity of many kinds of equipment. Consider too the losses in transmission-line and cable circuits. The economical value of these conductors is a function of the annual energy loss, which in turn may depend upon several variable factors. Kelvin's law, in its application, presumes "an equivalent mean-annual current."

Or consider traction motors, rolling-mill motors, hoist and crane-equipment, generators and transformers in the service of loads which fluctuate rapidly over wide values; about which it may well be said that no two load curves are likely to be duplicates. It would seem that methods are needed for giving quick and reliable solutions for the capacities and losses involved.

The purpose of this paper is to examine some features of these problems and to present a few methods which have been evolved for their solution, with the thought that they may be of interest, and perhaps stimulate further investigation in the field of short solutions to these important problems.

Basic Elements

Load curves, as commonly understood, involve the rate of change of some physical quantity plotted as ordinates versus units of consecutive time as abscissae. Examples of ordinates are kilowatts, horsepower, amperes, gallons per minute, passengers per hour, car loadings per day. In utility work the ordinates customarily represent a rate of change of energy, which results in the area under the curve becoming a measure of the energy involved. Such curves may be reduced to a common denominator for comparison of shapes by making the maximum value and total time in each case equal to unity, and plotting the lesser values as fractions of these maxima. This procedure permits direct reading of those factors which are also expressed in fractions of the maxima, and, being without physical dimensions, the scales on the axes of co-ordinates need be numerical only.

The essential elements in the building of a synthetic load curve are the following:

1. T, the time or duration of the load; herein primarily the time of some definite cycle, or the transition period between extreme values; which period could be some fraction of a cycle.
2. N, the minimum value of the load.
3. M, the maximum value of the load.

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After initial calibration it is often desirable to raise or lower the complete calibration curve in order to equalize the positive and negative errors. This is taken care of by the application of an adjusting arm which rotates the armature assembly; this adjustment is made with the instrument operating at any desired indication, thus permitting very accurate correction.

Addition of balance weights to the counterweight arms illustrated in Figure 5 permits adjustment to mechanically balance the armature assembly so that the instrument may be operated in any position.

Representative Data

Calibration curves for three representative tachometer indicators are shown in Figure 7; the solid curves are for increasing engine speeds, and the dotted curves are for decreasing engine speeds.

As explained in a previous paragraph, curve B of Figure 6 indicates the change in calibration for a typical indicator throughout the temperature range from −35 to +50 degrees centigrade.

Table II lists pertinent details for a representative tachometer installation consisting of two indicators and one generator.

Table II

Operating range	500–3,500 rpm (250–1,750 rpm generator speed)
Accuracy (+25 C)	±10 rpm
Generator weight	2.5 pounds
Indicator weight (each)	1.0 pounds
Indicator input (2,400 rpm)	5 watts
Position error	6 rpm
Magnetic effect (5 inches from compass needle)	0.5 degree
Indicator temperature rise (2,400 rpm)	35 C
Generator temperature rise (2,400 rpm)	20 C

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4. *W*, the energy involved in the period considered.

5. *A*, the average value of the load.

6. ϕ, the load factor; the ratio of the average load to the maximum load.

7. θ, the loss factor; the ratio of the average loss to the maximum loss, or loss at the maximum load.

8. *K*, the variation factor; the ratio of the minimum load to the maximum load.

9. *r*, the root-mean-squared value of the loads.

10. *u*, the capacity factor; the ratio of the root-mean-squared value of the loads to the average value.

11. ρ, the capacity factor squared, or the ratio of the average loss to the loss at the average load.

By definition, these elements are connected by the simple relations:

$$N=KM,\ W=AT,\ A=M\phi,\ r=uA,\ r^2=\rho A^2,\ r^2=M^2\theta,\ \theta=\rho\phi^2$$

Just as the load factor (ϕ) is a fraction denoting the ratio of the average load to the maximum load, so the loss factor (θ) is a fraction expressing the ratio of the average loss to the maximum loss, and the variation factor (*K*) is a fraction giving the ratio of the minimum load to the maximum load.

Capacity factor is a useful ratio, always greater than unity, for expressing *r* in terms of *A*. For example, a capacity factor of 1.20, as applied to a piece of equipment, would mean that its rating for the period which may be considered must exceed the average load carried by 20 per cent, to care for the I^2R losses under variable load.

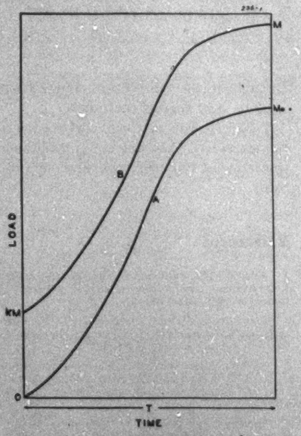

Figure 1. Curves of *f*(*t*) and *f*(*t*)+*KM*

Fundamental Equations

Referring to Figure 1, assume that *m* is a continuous function of *t* for curves *A* and *B*. These curves are identical in shape, so that for any value of *t* the corresponding values of *m* for the respective curves differ by *KM*.

Let curve *A* be defined by $m=f(t)$, then curve *B* is defined by $m=KM+f(t)$. Then, as shown in appendix A, the load factor and loss factor corresponding to curve *B* may be expressed respectively as

$$\phi=K+(1-K)\phi_0 \tag{1}$$

and

$$\theta=\phi^2+(\rho_0-1)(\phi-K)^2 \tag{2}$$

In the above equations the symbols without subscripts refer to curve *B*, while those with the zero subscripts refer to curve *A*. These equations are fundamental and apply to all types of continuous functions. Equation 2 expresses the loss factor (θ) as the load factor squared (ϕ^2) plus some fraction (ρ_0-1) of the square of the difference between the load factor and the variation factor $(\phi-K)^2$.

The value of the fraction (ρ_0-1) depends on the shape of the curve so that the factor (ρ_0) may be said to identify the curve form. By definition

$$\rho_0=\frac{\theta_0}{\phi_0^2}$$

and from equation 2

$$\rho_0-1=\frac{\theta_0}{\phi_0^2}-1=\frac{\theta-\phi^2}{(\phi-K)^2}$$

or

$$\rho_0=1+\frac{\theta-\phi^2}{(\phi-K)^2}$$

A more suitable way of expressing the load as a function of time is in the form: $m/M=f(t/T)$. The maximum values of m/M and t/T will then be unity or 1.0, occurring when $m=M$ and $t=T$.

The function of t/T may take a variety of forms. One which has proved most useful is given by $(t/T)^n$ which can be made to cover the parabolic types of power functions by using real positive values for *n*, and the hyperbolic types by rearranging the configuration and using negative values.[5]

The exponent (*n*) of $(t/T)^n$ is a function of the load factor (ϕ) and the variation factor (*K*), the relation being

$$n=\frac{1-\phi}{\phi-K} \tag{3}$$

Using $(t/T)^n$, the curve *B* of Figure 1 is expressed by

$$m=M\left[K+(1-K)\left(\frac{t}{T}\right)^{\frac{1-\phi}{\phi-k}}\right] \tag{4}$$

from which

$$\theta=\phi^2+\frac{(\phi-K)^2(1-\phi)^2}{(1-K)^2-(1-\phi)^2} \tag{5}$$

Thus is given a value for the loss factor (θ) in terms of the load factor (ϕ) and the variation factor (*K*). The path of the curve is determined by the values of *K* and ϕ, and the area under the curve is equal to the energy *W* ($W=\phi MT$).

For the special case in which *K* is equal to zero, the lower curve of Figure 1 applies and

$$\theta=\frac{\phi}{2-\phi} \tag{6}$$

$$\rho=\frac{1}{\phi(2-\phi)} \tag{7}$$

Derivations of equations 3 to 7 inclusive, are shown in appendix *B*. The curves of Figure 2 give values of $(t/T)^n$ for values of *n* varying from 0.05 to 20.0. In Figure 3 are given curves of ϕ, θ and ρ as functions of *n* for two values of *K*, namely $K=0$ and $K=0.2$. In Figure 4 are given curves of ρ and θ as functions of ϕ for five values of *K*.

There is an endless variety of functions to which equation 1 may be applied. A

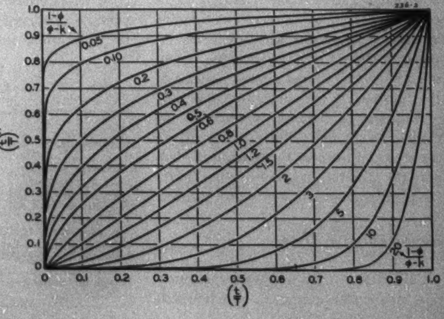

Figure 2. Curves showing $(t/T)^n$ where $n=\dfrac{1-\phi}{\phi-k}$

Table I

No.	Description	Equation of Curve (m/M)	Load Factor ϕ	$\rho_0 = \Theta_0/\phi^2$	Loss Factor (Θ)	Minimum Value of ϕ
1	Straight Line	$K + (1-K)\, t/T$	$\frac{1+K}{2}$	$4/3$	$1/3(1+K+K^2)$ or $\phi^2 + 1/3(1-\phi)^2$	0.5
2	Parabolic Power	$K + (1-K)(\frac{t}{T})^n \quad n = \frac{1-\phi}{\phi-K}$	$\frac{1+Kn}{n+1}$	$\frac{1}{\phi_0(2-\phi_0)}$	$\phi^2 + (\phi-K)^2\left[\frac{1}{(\frac{1-K}{1-\phi})^2 - 1}\right]$	0
3	Exponential	$K + (1-K)\left(\frac{e^{t/T} - 1}{e - 1}\right)$	$0.418 + 0.582K$	1.46912	$\phi^2 + 0.4691(\phi-K)^2$ or $\phi^2 + 0.2397(1-\phi)^2$	0.418
4	Sine	$K + (1-K)\, \mathrm{Sin}\, \frac{\pi}{2}(t/T)$	$K + 2/\pi\,(1-K)$	$\pi^2/8$	$\phi^2 + 0.2337(\phi-K)^2$ or $\phi^2 + 0.7173(1-\phi)^2$	$2/\pi = 0.6366$
5	Versine	$K + 1/2(1-K)\, \mathrm{vers}\, \pi\,(t/T)$	$\frac{1+K}{2}$	$3/2$	$\phi^2 + 1/2(\phi-K)^2$ or $\phi^2 + 1/2(1-\phi)^2$	0.5
6	Ellipse or Circle	$K + (1-K)\sqrt{1-(t/T)^2}$	$K + \frac{\pi}{4}(1-K)$	$3^2/3\pi^2$	$\phi^2 + 0.0808(\phi-K)^2$ or $\phi^2 + 1.082(1-\phi)^2$	$\pi/4 = 0.7854$
7	Witch	$(K/1-K)\left[\frac{1}{(t/T)^2 + \frac{K}{1-K}}\right]$	$\sqrt{\frac{K}{1-K}}\, \mathrm{Cos}^{-1}\sqrt{K}$	$K \neq 0$	$\frac{\phi+K}{2}$	0.4163 $K = 0.1$
8	Hyperbolic Secant.	$\mathrm{Sech}\left[(t/T)\, \mathrm{Sech}^{-1} K\right]$	$\frac{\mathrm{Cos}^{-1} K}{\mathrm{Sech}^{-1} K}$	$K \neq 0$	$\phi\left(\frac{\sqrt{1-K^2}}{\mathrm{Cos}^{-1} K}\right)$	0.4913 $K = 0.1$
9	Power – 3rd Degree (Slope = 0 for t = 0 & t = T)	$K + 3(1-K)(t/T)^2 + 2(1-K)(t/T)^3$	$\frac{1+K}{2}$	$52/35$	$\phi^2 + 0.4857(\phi-K)^2$ or $\phi^2 + 17/35(1-\phi)^2$	0.5
10	Inverted Parabola	$1 - (1-K)(t/T)^2$	$1/3(K+2)$	$6/5$	$\phi^2 + 0.2(\phi-K)^2$ or $\phi^2 + 0.8(1-\phi)^2$	$2/3$
11	Power – 2nd Degree	$K + 2(3\phi - 2K - 1)(t/T) + 3(1 + K - 2\phi)(t/T)^2$			$\phi^2 + 1/15\left[2(\phi-K)^2 + (1-\phi)^2 + (1-\phi)(1-K)\right]$ or $\phi^2 + 1/15\left[3(\phi-K)^2 - 3(1-\phi)(\phi-K) + 2(1-K)^2\right]$	
12	Power Series – 3rd Degree (Slope = 0 for t = 0)	$K + \left[12(\phi-K) - 3(1-K)\right](t/T)^2 + \left[-12(\phi-K) + 4(1-K)\right](t/T)^3$			$\phi^2\ 1/35\left[12(\phi-K)^2 + (1-\phi)^2 + 2(1-\phi)(1-K)\right]$ or $\phi^2 + 1/35\left[13(\phi-K)^2 - 4(1-K)(\phi-K) + 3(1-K)^2\right]$	
13	Power Series – 4th Degree (Slope = 0 for t = 0 & t = T)	$K + 6\left[5(\phi-K) - 2(1-K)\right](t/T)^2 + 4\left[-15(\phi-K) + 7(1-K)\right](t/T)^3 + 15\left[2(\phi-K) - (1-K)\right](t/T)^4$			$\phi^2 + 1/35\left[8(\phi-K)^2 + 7(1-\phi)^2 + (1-\phi)(1-K)\right]$ or $\phi^2 + 1/35\left[15(\phi-K)^2 - 15(1-K)(\phi-K) + 8(1-K)^2\right]$	
14	Power Series – 3rd Degree Slope = 0 for t = T	$K + \left[12(\phi-K) - 6(1-K)\right](t/T) + \left[15(\phi-K) - 24(1-K)\right](t/T)^2 + \left[12(\phi-K) - 8(1-K)\right](t/T)^3$			$\phi^2 + 1/35\left[3(\phi-K)^2 + 10(1-\phi)^2 + 2(1-\phi)(1-K)\right]$ or $\phi^2 + 1/35\left[13(\phi-K)^2 - 22(1-\phi)(1-K) + 12(1-K)^2\right]$	
15	Based on Frequency Curve	$K + (2/\sqrt{\pi})(\phi-K)(\log_e T/t)^{1/2}$ or $t/T = e^{-\frac{(m/M - K)^2 \pi}{4(\phi-K)^2}}$		$4/\pi$	$\phi^2 + 0.27324(\phi-K)^2$	
16	Power Sine (ϕ; K & Θ Given)	$K + 2(3\phi - 2K - 1)(t/T) + 3(1 - K - 2\phi)(t/T)^2 + C_1 \,\mathrm{Sin}\, 2\pi\, t/T$ where $C_1 = \frac{1-K}{\pi}\sqrt{(\frac{1-K}{\pi})^2 - 2\phi^2 + 2\Theta - 2/15\left[2(\phi-K)^2 + (1-\phi)^2 + (1-\phi)(1-K)\right]}$				
17	Horizontal Parabola (See No. 2)	$K + (1-K)(t/T)^{1/2}$	$1/3(K+2)$	$9/8$	$\phi^2 + 0.125(\phi-K)^2$ or $\phi^2 + 0.5(1-\phi)^2$	$2/5$
18	Hypocycloid	$K + (1-K)\left[1 - (t/T)^{2/3}\right]^{1/3}$	$K + 3\pi/32(1-K)$	$\frac{16384}{945\pi^2} = 1.75667$	$\phi^2 + 0.75667(\phi-K)^2$ or $\phi^2 + 0.13188(1-\phi)^2$	$3\pi/32 = 0.2945$

few have been tabulated in Table I. The accuracy desired may require that thought be given to apply the function best suited to the problem in hand, and in this connection it should be kept in mind that the lower limit which may be used for the load factor (ϕ) is determined by the load factor (ϕ_0) of the basic function. For example, for the parabolic type of curve represented by equation 4, the load factor (ϕ) may have any value from zero to unity. When the load factor exceeds the value of 0.5, the versine and hyperbolic-secant functions often apply. A little practice will lead to the proper selection upon inspection of the conditions.

Referring to Table I it will be noted that for some of the functions, two expressions are indicated for the loss factor (θ), one being a function of ($\phi-K$) and the other a function of ($1-\phi$), which permits the calculation of two values for the loss factor. By taking an average of the two, a closer value of the loss factor can be obtained. If the curve fits the conditions exactly, the two will be equal in value.

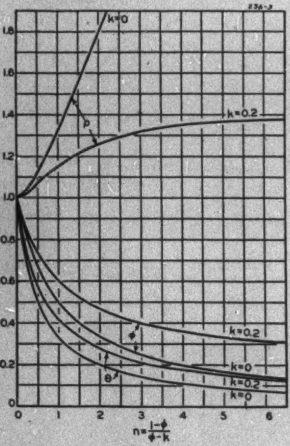

Figure 3. Curves showing variations of ϕ, θ and ρ in terms of n, where $n=\dfrac{1-\phi}{\phi-k}$

Application to Power-Station Curves

Referring to Figure 5, an industrial load which covers one day's output from a power station is represented by curve *A*. By rearranging the load values in an increasing series, curve *B* is obtained. The two curves are equivalent for present purposes, for it is immaterial in what order the values occur so long as the magnitudes and corresponding durations remain unchanged. For both curves, the maximum load, the minimum load, the load factor, and the loss factor are identical.

It is entirely possible to construct synthetic curves having the same values of W, T, ϕ, ρ, and θ as have curves *A* and *B*, such as represented by curve *C* which is defined by formula 16 of Table I. However, this is not ordinarily the problem. What is desired are values for θ and ρ corresponding to the given values of ϕ and K for curves *A* and *B*. The accuracy with which the former can be determined will be governed by the formula selected from Table I to represent curve *B* of Figure 5. For example, using equation 5 above, which corresponds to formula 2 of Table I, a value of 0.344 is obtained for θ which is 5.5 per cent lower than the actual value of 0.364. It is to be noted that the slope of curve *B* is approximately zero at the lower and upper limits. For this condition, formula 13 of Table I would apply, from which a value of 0.366 is obtained for θ which is only 0.55 per cent above the actual value and therefore within acceptable engineering accuracy.

Along the same line consider curve *A* of Figure 6 which depicts a utility load curve of different shape. Again, curve *B* is curve *A* redrawn. The actual factors for these curves are $K=0.150$; $\phi=0.639$; and $\theta=0.478$. Using equation 5 above, a value of 0.461 is calculated for θ which is 3.6 per cent low. Since the value of ϕ exceeds 0.5, and considering the shape of curve *B*, formulas 14 and 15 of Table I are suggested. With the former, the value obtained for θ is 0.484, which is 1.2 per cent high; with the latter a value of 0.474 is obtained which is 0.8 per cent low. The graph for formula 14 is represented by curve *D*.

In both Figures 5 and 6, the curves *B* have been obtained by tabulating the average values of m for small intervals of t, and then arranging these values consecutively in ascending order. Since the area under each curve is $\int(m)dt$, or equally, $\int(t)dm$, the same result may be obtained by taking average values of t for small intervals of m. In either case, the resulting curve is the well-known load-duration curve, with the scale of abscissae reversed in direction.

Load-Curve Combinations

When daily load curves for a given system follow the same general pattern, one method of approach for the consideration of a longer period such as a month or year, is illustrated in Figure 7. The solid shown is obtained by arranging the days, not chronologically, but in the order of increasing energy values, so that the area of the front face measures the energy on the minimum day and that of the rear face, the energy of the maximum day. The volume then measures the energy for the total period. The hours in a single day are represented by T_d and the days in the period by T_y.

By making use of geometrical ratios many relations can be established between the daily and yearly factors involved, depending upon the nature of the problem under consideration. If the left-hand curves of Figure 8 are considered as representing two of the 365 daily load curves for a period of one year, the others lying between these two, then the daily energy values can be represented as shown by the right-hand curve. The area under the latter measures the annual energy. The various formulas which have heretofore been considered can be applied, requiring only the substitution of the energy value W for the maximum load value M in the appropriate formulas.

An able exposition of the weekly and seasonal variations in load curves and their components, classified as to time of day and character of load, has been given by Farley C. Ralston. To those readers interested in problems having to do with predictions of future demand and energy, his paper[3] is particularly recommended.

The Use of Probable Values

Another method for handling longer periods, which is perhaps more satisfactory, may be developed from the characteristics of a single curve, such as the one shown in Figure 9. This indicates the total hours during which the load reaches the value indicated.

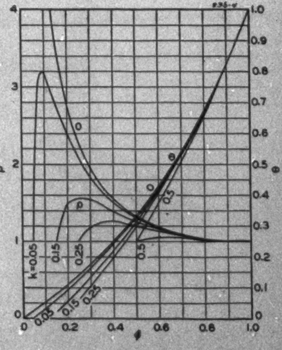

Figure 4. Curves showing values of θ and ρ in terms of ϕ for various values of k

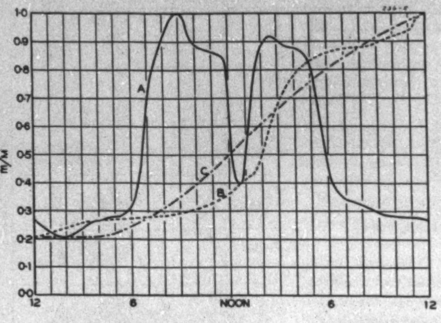

Figure 5. Industrial load curve

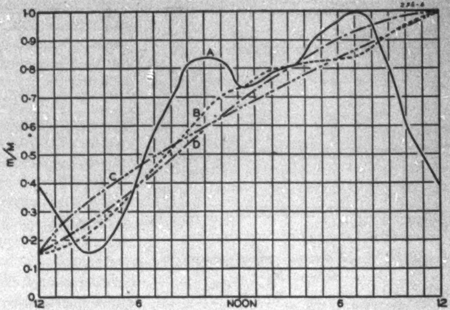

Figure 6. Utility load curve

Experience has demonstrated that, when a considerable number of days are combined in this manner, the curve tends to assume the general shape shown. This is due to the fact that extremely low and high values of load do occur, but less frequently than those nearer to the average value. This circumstance leads naturally to a comparison with the well-known frequency-curve as given in Figure 10. The mode of the latter corresponds to the time (T). Referring to Figure 9, the probability, or duration, of a load not less than C in value is measured by T_1, corresponding to P_1 in Figure 10, and therefore C corresponds to Q in value. In other words the probability of the load's being at least KM in value is unity; of its being M or greater, say $^1/_{48}$ or one-half hour each day, and between these two, as shown for intermediate values.[6,7]

The curve of Figure 9 is, in reality, a load-duration curve reduced to daily values. If a yearly period be covered, then by dividing each value of abscissae, given in hours, by 365, the curve represents a daily period, which, however, includes all values of the load encountered throughout the year; and p represents the time by which the daily peak is measured. Otherwise stated, if a one-hour peak be considered, $p=1.0$ for $T=24$ hours or $p={}^1/_{24}$ for $T=1.0$.

Should a maximum be so rare that its value is reached for one hour only during the year, then the value of p would be 1/8,760 for $T=1.0$. So p may be considered as a measure of the probability that the load will not be less than the value corresponding to D.

The equation for the load-duration curve of Figure 9 is

$$m=M\left[K+\frac{2}{\sqrt{\pi}}(\phi-K)\left(\log_e\frac{T}{t}\right)^{1/2}\right] \tag{8}$$

and the loss factor corresponding thereto is defined by

$$\begin{aligned}\theta&=\phi^2+\left(\frac{4}{\pi}-1\right)(\phi-K)^2\\&=\phi^2+0.27324(\phi-K)^2\end{aligned} \tag{9}$$

where

$$\rho_0=\frac{4}{\pi}$$

This is a most useful form for application to utility loads and appears to increase in accuracy of results as the time under consideration increases.

Maximum-demand instruments will usually read the peak load as an average during the time considered, in which case the relations hold as given. Referring to Figure 9, the curve is sometimes considered as having the value (D) for the given period (p). For this condition the equations for the curve and corresponding loss factor are expressed by

$$m=M\left[K+(1-K)\left(\frac{\log_e\frac{T}{t}}{\log_e\frac{T}{p}}\right)^{1/2}\right] \tag{10}$$

$$\theta=\phi^2+\left(\frac{4}{\pi}-1-\frac{cp}{T}\right)(\phi-K)^2 \tag{11}$$

With reference to Figure 9, equation 10 applies between the limits for $t=p$ and $t=T$. Between the limits for $t=0$ and $t=p$ it is obvious that $m=M$ or $m/M=1.0$.

The value of c in equation 11 is a complicated function of p/T. Values of c can be had from curve B of Figure 13 for values of p/T up to 0.1, which is equivalent to 144 minutes in a 24-hour period. The term cp/T is empirical to make equation 11 similar in character to equation 9, the change in the value of θ being small for small values of p/T.

The derivations of these equations are included as appendixes C and D.

Comparison of Maximum Demands

The curve of Figure 9 may also be used as a basis for converting a peak demand, averaged for a given time interval, to some other time interval. For example, suppose a peak of M_2 is metered on a demand of t_2 minutes. Its value metered on a demand of t_1 minutes could be expressed by

$$M_1=N+(M_2-N)\frac{g_1}{g_2} \tag{12}$$

The fraction g_1/g_2 is also a complicated function of p/T. Values of g_1/g_2 which are commonly used and can be substituted in equation 12 are given in Table II. It is to be noted that when $N=0$, the peaks

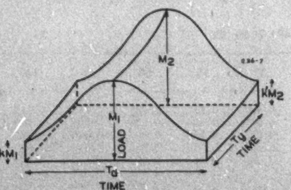

Figure 7. Grouping of daily load curves represented by a solid

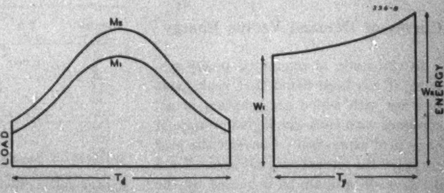

Figure 8. Grouping of daily load curves represented by an area

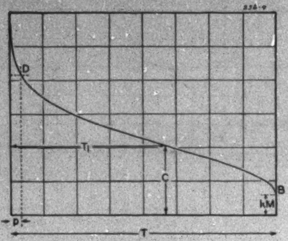

Figure 9. Load-duration curve reduced to daily values

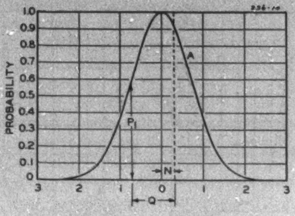

Figure 10. Probability curve

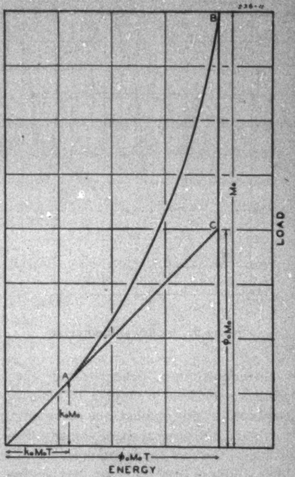

Figure 11. Peak-load versus energy curve

are in direct ratio to the value of g_1/g_2. Relations other than those of Table II may be obtained by reading the values of g_1 and g_2 from curve *A* of Figure 13. The derivation of equation 12 is shown in appendix E.

Utility Distribution Losses

The losses attending a diversified distribution of energy can, as is well-known, be classified into two principal groups: those remaining practically constant, such as transformer-core losses, leakage, dielectric, and corona losses; and those which vary as the square of the current at each point and are known from experience to vary, practically, as the square of the total load.

Over a determined period, the sum of these two groups is known from meter readings at the points of generation and reception. If it happens in the two selected periods that the load factors and the average values of the losses differ sufficiently, then the loss factors determined in one of the manners outlined, furnish a method for segregating these variable and fixed losses.

Let L_1 = average loss corresponding to θ_1 and M_1
L_2 = average loss corresponding to θ_2 and M_2
L_0 = constant loss

Then as derived in appendix F

$$L_0=L_2-\frac{L_2-L_1}{1-\frac{\theta_1}{\theta_2}\left(\frac{M_1}{M_2}\right)^2} \qquad (13)$$

Curves of Demand Versus Energy

In the study of integrated power systems, it has been found that peak loads increase with added consumption in accordance with laws which give promise of analytical expression. Consider the load curves of the form shown in Figures 5 and 6. If each ordinate be increased by the same percentage, the total area, or energy, will be increased by an equal percentage, and the load factor will remain unchanged. Also, if the lower values are increased in general a greater percentage than is the percentage increase in the peak value, the resulting increase in energy will be accompanied by an increase in load factor. This latter type of growth is the desideratum of all utility men, but unfortunately the opposite seems more often to happen, namely, the peak increases at a more rapid rate than the energy, with a resulting decrease in load factor.

These relations can be analyzed with reference to the well-known load-energy curve illustrated in Figure 11. Referring to the latter, ϕ_0 is the load factor for a fixed period (T). The variation-factor is K_0. With the maximum value of M_0 and T as unity, the distance between C and B will be equal to $1-\phi_0$. Some line joining A and B will be the locus of the peak loads with variation of consumption.

Experience with growth in California lead engineers in that state to the belief that the curve from A to B is a parabola, tangent to AC at A and tangent to CB at B. Also, they and others feel that all load-energy curves can be expressed in this parabolic form.[1,2]

A general equation of the curve of Figure 11 derived in appendix G, may be given by:

$$\phi=1-(1-\phi_0)\left[\frac{(W/M_0T)-K_0}{\phi_0-K_0}\right]^\beta \qquad (14)$$

where the exponent β can be determined for the specific case by obtaining values of M and the associated values of W, other than those of the terminal points.

Some specific applications are illustrated in Figure 12. The load-energy curves are shown as full lines and the corresponding load-factor curves as dotted lines. Curve D is a parabola with points of tangency at A and B. Curve E indicates the relations for a linear increase of peak load with energy. Curve F is obtained for $\beta=1.0$. It is to be noted that the line O-C in Figures 11 and 12 represents the diagonal of a square of which ϕ_0 is one side.

In the Philadelphia district, it was determined a few years ago, that the growth in energy output and maximum yearly peak could be expressed closely by the same constant percentage increase. This condition would mean a constant yearly load factor. With reference to Figure 12, this condition, which represents a stabilization of the change in load factor, means that the curve E' will have become horizontal at some point beyond C, and the curve E continue beyond the

Table II. Values of g_1/g_2

Minutes	5	10	15	20	30	45	60	90	120
5	1.000	0.945	0.911	0.889	0.852	0.817	0.788	0.747	0.717
10		1.000	0.964	0.940	0.902	0.864	0.834	0.791	0.759
15			1.000	0.975	0.935	0.896	0.865	0.820	0.787
20				1.000	0.959	0.919	0.886	0.841	0.807
30					1.000	0.958	0.924	0.877	0.842
45						1.000	0.965	0.915	0.878
60							1.000	0.948	0.910
90								1.000	0.960
120									1.000

Multiply when converting from minutes at left to minutes at top. Divide for the reverse operation.

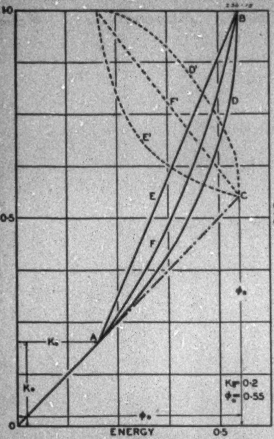

Figure 12. Peak-load versus energy and load-factor versus energy curves

point B as a straight line with less slope than the curve E has at B. In fact, this result would make curve E slightly concave downward at B.[3]

Combination of Equal Factors

In this discussion of load factors, it may not be amiss to rewrite two formulas which have proved most useful in determining the combined load factors or loss factors of a number of loads (Q in number) which occur at random and are about equal as to load factors, magnitudes, and durations.[4]

$$\phi_Q = \frac{1}{Q^{-1/\alpha}\left(\frac{1}{\phi_1}-1\right)+1} \tag{15}$$

$$\theta_Q = \frac{1}{Q^{-1/\psi}\left(\frac{1}{\phi_1}-1\right)+1} \tag{16}$$

For a uniform distribution of loads throughout the period considered, it would seem that the values of α and ψ should have a theoretical value of 2, to harmonize with the theory of least squares as applied to deviations. The author has found from experience that chiefly due to a nonuniform load distribution, over the time considered—such as a day—the values of α and ψ will be greater than 2. The actual values, once found for specific conditions, can be re-employed for others, in which the magnitudes involved may be quite different.

These formulas, the derivation of which is shown in appendix H, may be applied to many problems involving demand factors and diversity factors and have been found particularly helpful in the design of utility distribution systems, whether for electricity, gas, water, or sewage.

Conclusions

The formulas presented in this paper may be applied in many ways, and their effectiveness enhanced by the addition of constants derived from experience in actual calculations. Here, they demonstrate methods only.

Often comparison between the behavior of small loads and large loads is lost sight of through the differences in magnitudes and scales used. It is felt that a reduction to unity values is a first step in arriving at a greater simplicity in the visualization of essential factors.

In passing from the specific case to the more general, in which the elements of probability enter, it is believed that more useful and accurate solutions are obtained, especially in that field in which the elements have an economic significance which depends on yearly values.

There are many routes by which one may arrive at answers for the intangible problems to which these formulas are best suited. It is possible even to obtain rms values of loads drawn on circular charts by weighing the aggregate amount of paper included within the graphs; or the same results often may be gotten by instruments and meters. If this presentation assists some engineers in reducing the detailed labor of plotting, planimeter work, and summations, one of its purposes will have been accomplished.

To explain more fully direct methods of application, the solutions to some simple examples are included as appendix I.

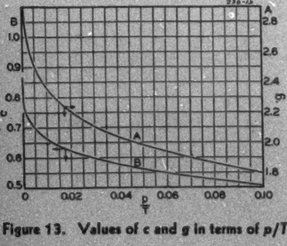

Figure 13. Values of c and g in terms of p/T

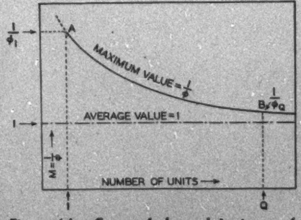

Figure 14. Curve of demand in terms of number of units contributing

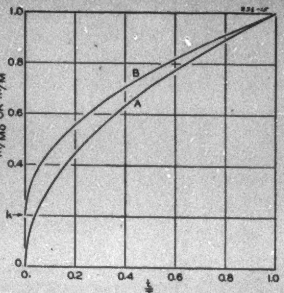

Figure 15. Curves of $f(t/T)$ and $f(t/T)+k$

Appendix A

Referring to Figure 1 of the paper, the equation for curve A can be expressed as

$$m = f(t)$$

then

$$W_0 = \int^T f(t)dt$$

and

$$\phi_0 = \frac{W_0}{M_0T} = \frac{1}{M_0T}\int_0^T f(t)d$$

or

$$\int_0^T f(t)dt = \phi_0 M_0 T \tag{17}$$

for curve B

$$m = KM + f(t)$$

and

$$W = \int_0^T [KM + f(t)]dt$$

$$\phi = \frac{W}{MT} = \frac{1}{MT}\int_0^T [KM+f(t)]dt$$

$$= K + \frac{1}{MT}\int_0^T f(t)dt \tag{18}$$

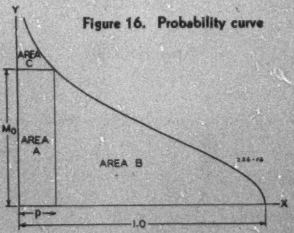

Figure 16. Probability curve

substituting equation 17 in equation 18

$$\phi=K+\phi_0\frac{M_0}{M} \tag{19}$$

but

$$M=M_0+KM \text{ or } M_0=(1-K)M \text{ and}$$

$$\frac{M_0}{M}=(1-K)$$

therefore

$$\phi=K+\phi_0(1-K) \tag{20}$$

By definition

$$\theta_0=\frac{1}{M_0{}^2T}\int_0^T[f(t)]^2dt \tag{21}$$

and

$$\theta=\frac{1}{M^2T}\int_0^T[KM+f(t)]^2dt \tag{22}$$

from which

$$\theta=\frac{1}{M^2T}\left[K^2M^2T+2KM\int_0^T f(t)dt+\int_0^T[f(t)]^2dt\right] \tag{23}$$

from equation 21

$$\int_0^T[f(t)]^2dt=\theta_0M_0{}^2T \tag{24}$$

substituting equations 17 and 24 in equation 23 and since

$$\frac{M_0}{M}=(1-K)$$

$$\theta=K^2+2K(1-K)\phi_0+(1-K)^2\theta_0 \tag{25}$$

from equation 20

$$\phi_0=\frac{\phi-K}{1-K}$$

and by definition

$$\rho_0=\frac{\theta_0}{\phi_0{}^2}$$

Therefore

$$\theta_0=\rho_0\phi_0{}^2=\rho_0\left(\frac{\phi-K}{1-K}\right)^2$$

substituting in equation 25

$$\theta=K^2+2K(\phi-K)+\rho_0(\phi-K)^2$$

from which

$$\theta=\phi^2+(\rho_0-1)(\phi-K)^2 \tag{26}$$

Appendix B

By plotting the curves of Figure 1 with

$$\frac{m}{M_0}=f\left(\frac{t}{T}\right)$$

and

$$\frac{m}{M}=K+f\left(\frac{t}{T}\right)$$

as shown in Figure 15, then if

$$f\left(\frac{t}{T}\right)=\left(\frac{t}{T}\right)^n$$

equations for curves *A* and *B* become respectively,

$$\frac{m}{M_0}=\left(\frac{t}{T}\right)^n \text{ and } \frac{m}{M}=K+(1-K)\left(\frac{t}{T}\right)^n$$

$$W_0=\int_0^1\left(\frac{t}{T}\right)^n d\left(\frac{t}{T}\right)=\frac{1}{n+1} \tag{27}$$

when

$$m=M_0=1.0, \quad t=T_0=1.0$$

$$\phi_0=\frac{W_0}{M_0T_0}=W_0=\frac{1}{n+1} \tag{28}$$

Since

$$\phi=K+(1-K)\phi_0 \text{ (from appendix A)}$$

$$\phi=K+\frac{(1-K)}{(n+1)} \text{ from which } n=\frac{1-\phi}{\phi-K} \tag{29}$$

Then

$$\frac{m}{M}=K+(1-K)\left(\frac{t}{T}\right)^{\frac{1-\phi}{\phi-K}}$$

or

$$m=M\left[K+(1-K)\left(\frac{t}{T}\right)^{\frac{1-\phi}{\phi-K}}\right] \tag{30}$$

by definition

$$\theta_0=\int_0^1\left(\frac{t}{T}\right)^{2n}d\left(\frac{t}{T}\right)=\frac{1}{2n+1} \tag{31}$$

and from appendix A

$$\theta=\phi^2+(\rho_0-1)(\phi-K)^2$$

also by definition

$$\rho_0=\frac{\theta_0}{\phi_0{}^2}=\frac{1}{2n+1}\div\frac{1}{(n+1)^2}=\frac{n^2}{2n+1}+1$$

or

$$\rho_0-1=\frac{n^2}{2n+1}$$

so that

$$\theta=\phi^2+\left(\frac{n^2}{2n+1}\right)(\phi-K)^2$$

or

$$\theta=\phi^2+\frac{(1-\phi)^2(\phi-K)^2}{(1-K)^2-(1-\phi)^2} \tag{32}$$

Since

$$n=\frac{1-\phi}{\phi-K}$$

For the special case where $K=0$

$$\theta=\phi^2+\frac{(1-\phi)^2\phi^2}{1-(1-\phi)^2}=\frac{\phi}{2-\phi} \tag{33}$$

and

$$\rho=\frac{\theta}{\phi^2}=\frac{\phi}{(2-\phi)\phi^2}=\frac{1}{\phi(2-\phi)} \tag{34}$$

Appendix C

A common form for the equation of the probability curve shown in Figure 10 is

$$y=e^{-h^2x^2}$$

from which

$$x=\frac{1}{h}\left(\log_e\frac{1}{y}\right)^{1/2} \tag{35}$$

Let

$$x=\frac{m}{M_0}$$

and

$$y=\frac{t}{T}$$

then equation 35 becomes

$$\frac{m}{M_0}=\frac{1}{h}\left(\log_e\frac{T}{t}\right)^{1/2} \tag{36}$$

One half of the area under the probability curve is

$$\int y\,dx=\int_0^\infty e^{-h^2x^2}dx=\frac{\sqrt{\pi}}{2h} \tag{37}$$

which is also the area corresponding to W_0. From appendix *B*

$$W_0=\phi_0$$

and from appendix *A*

$$\phi_0=\frac{\phi-K}{1-K}$$

Therefore

$$W_0=\phi_0=\frac{\phi-K}{1-K}=\frac{\sqrt{\pi}}{2h}$$

from which

$$\frac{1}{h}=\frac{2}{\sqrt{\pi}}\left(\frac{\phi-K}{1-K}\right)$$

substituting in equation 36

$$\frac{m}{M_0}=\frac{2}{\sqrt{\pi}}\left(\frac{\phi-K}{1-K}\right)\left(\log_e\frac{T}{t}\right)^{1/2} \tag{38}$$

from appendix B

$$\frac{m}{M}=K+(1-K)\frac{m}{M_0}$$

so that

$$\frac{m}{M}=K+\frac{2}{\sqrt{\pi}}(\phi-K)\left(\log_e\frac{T}{t}\right)^{1/2}$$

or

$$m=M\left[K+\frac{2}{\sqrt{\pi}}(\phi-K)\left(\log_e\frac{T}{t}\right)^{1/2}\right] \tag{39}$$

for

$$m=M_0$$

and

$$t=T$$

$$\theta_0=\int_0\left(\frac{m}{M_0}\right)^2dt$$

or

$$\theta_0=\frac{1}{h^2}\int_0^1\left(\log_e\frac{T}{t}\right)d\left(\frac{t}{T}\right)$$
$$=-\frac{1}{h^2}\int_0^1\left(\log_e\frac{t}{T}\right)d\left(\frac{t}{T}\right)=\frac{1}{h^2}$$

then

$$\rho_0=\frac{\theta_0}{\phi_0^2}=\frac{1}{h^2}\div\left(\frac{\sqrt{\pi}}{2h}\right)^2=\frac{4}{\pi}$$

and

$$(\rho_0-1)=\left(\frac{4}{\pi}-1\right)$$

and

$$\theta=\phi^2+(\rho_0-1)(\phi-K)^2$$

or

$$\theta=\phi^2+\left(\frac{4}{\pi}-1\right)(\phi-K)^2 \qquad (40)$$

With the curve of Figure 9 terminating at point D, when

$t=p,\ m=D=M_0+KM$

so that equation 36 becomes

$$\frac{m}{M_0}=\frac{M_0}{M_0}=1.0=\frac{1}{h}\left(\log_e\frac{T}{p}\right)^{1/2} \qquad (41)$$

for the origin at $Pt\ O,\ KM$ from which

$$h=\left(\log_e\frac{T}{p}\right)^{1/2}$$

substituting in equation 36

$$\frac{m}{M_0}=\frac{\left(\log_e\frac{T}{t}\right)^{1/2}}{\left(\log_e\frac{T}{p}\right)^{1/2}}=\left[\frac{\log_e\frac{T}{t}}{\log_e\frac{T}{p}}\right]^{1/2}$$

also

$$\left[\frac{\log_{10}\frac{T}{t}}{\log_{10}\frac{T}{p}}\right]^{1/2} \qquad (42)$$

and

$$\frac{m}{M}=K+(1-K)\frac{m}{M_0}=K+(1-K)\left[\frac{\log_e\frac{T}{t}}{\log_e\frac{T}{p}}\right]^{1/2}$$

from which

$$m=M\left[K+(1-K)\left(\frac{\log_e\frac{T}{t}}{\log_e\frac{T}{p}}\right)^{1/2}\right]$$
$$=M\left[K+(1-K)\left(\frac{\log_{10}\frac{T}{t}}{\log_{10}\frac{T}{p}}\right)^{1/2}\right] \qquad (43)$$

between the limits $t=p$ and $t=T$.

Appendix D

Derivation of values for c and g of equations 11 and 12 for a peak-load period of one hour in 24 hours ($p=1/24$). From appendix C the equation for the probability curve (Figure 16) may be expressed as

$$y=\frac{1}{h}\left(\log_e\frac{1}{x}\right)^{1/2} \qquad (44)$$

where $y=M_0$ and $x=t/T$ then

$$\text{area } B=\frac{1}{h}\int_{x=p}^{z=1}\left(\log_e\frac{1}{x}\right)^{1/2}dx$$

let

$$z=\left(\log_e\frac{1}{x}\right)^{1/2}=\left(\log_e\frac{1}{p}\right)^{1/2}$$

when $x=p$. Therefore

$$\text{area } B=\frac{2}{h}\left[\left(\frac{z^3}{3}+\frac{z^7}{14}+\frac{z^{11}}{264}+\ldots\right)-\left(\frac{z^5}{5}+\frac{z^9}{54}+\frac{z^{13}}{1{,}560}+\ldots\right)\right]=\frac{0.801579}{h} \qquad (45)$$

$$\text{area } (A+B+C)=\frac{\sqrt{\pi}}{2h}=\frac{0.886227}{h}$$

(from appendix C) (46)

then

area $(A+C)$ = equation 46 less equation 45

$$=\frac{0.084648}{h}$$

Defining g as the average load during the period p, the value of g is obtained from

$$g=\frac{\text{area }(A+C)}{p}=\frac{24\times 0.084648}{h}=\frac{2.031552}{h} \qquad (47)$$

Since the objective is to obtain a ratio for two values of g, there is no need to obtain a numerical value for h which cancels out, or one can use $h=1.0$. The numerator of equation 47 is therefore one point on curve A of Figure 13 which has been plotted for the value

$p=1/24$ or 0.04167

From equation 44

$$y^2=\frac{1}{h^2}\log_e\frac{1}{x}$$

and the area under this curve corresponding to section B =

$$\int_p^1\frac{1}{h^2}\left(\log_e\frac{1}{x}\right)dx=\frac{1}{h^2}\left[1-p\left(1+\log_e\frac{1}{p}\right)\right]$$

and for $p=1/24$

$$r_B^2=\frac{0.825914}{h^2} \qquad (48)$$

From equation 44

$$M_0=\frac{1}{h}\left(\log_e\frac{1}{x}\right)^{1/2}=\frac{1}{h}\left(\log_e\frac{1}{p}\right)^{1/2}$$

and for $p=1/24$

$$M_0=\frac{1.782712}{h}$$

and

$$M_0^2=\frac{3.178062}{h^2}$$

so that the corresponding area under M_0^2 for section A becomes

$$r_A^2=M_0^2p=\frac{3.178062}{h^2}\times\frac{1}{24}=\frac{0.132419}{h^2} \qquad (49)$$

$$r_A^2+r_B^2=\frac{0.825914}{h^2}+\frac{0.132419}{h^2}=\frac{0.958333}{h^2} \qquad (50)$$

by definition

$$r^2=M^2\theta$$

so that

$$\theta_{A+B}=\theta_0=\frac{r_A^2+r_B^2}{M_0^2}=\frac{0.958333}{M_0^2h^2}$$

by definition

$$\phi=\frac{W}{MT}$$

and for $T=1.0$

$$\phi=\frac{W}{M}$$

then

$$\phi_{A+B}=\phi_0=\frac{W_A+W_B}{M_0}$$

$$W_A=M_0p=\frac{1.782712}{h}\times\frac{1}{24}=\frac{0.074280}{h}$$

$W_B=\dfrac{0.801579}{h}$ from equation 45

Therefore

$$\phi_{A+B}=\phi_0=\left(\frac{0.074280}{h}+\frac{0.801579}{h}\right)\frac{1}{M_0}$$
$$=\frac{0.875859}{M_0h}$$

$$\rho_0=\frac{\theta_0}{\phi_0^2}=\frac{0.958333}{M_0^2h^2}\times\frac{M_0^2h^2}{(0.875859)^2}$$
$$=1.249248 \qquad (51)$$

and

$$\rho_0-1=1.249248-1.0=0.249248$$

From appendix C $(\rho_0-1)=(4/\pi-1)$ for curve without constant value for M_0 during period p. As a correction for effect of latter condition, assume

$$(\rho_0-1)=\left(\frac{4}{\pi}-1\right)-\frac{cp}{T}$$

where cp/T is correction factor. Then

$$(\rho_0-1)=0.249=\left(\frac{4}{\pi}-1\right)-\frac{cp}{T}$$

and for $p/T=1/24$

$c=0.5758$ which represents one point on curve B of Figure 13 which has been plotted for the value $p=1/24$ or 0.042.

Appendix E

Referring to Figure 9, let p_1 and p_2 be two intervals during which the corresponding peak loads M_1 and M_2 are measured as average values. Then from appendix D

$$g_1 = M_1 - KM = M_1 - N$$

and

$$g_2 = M_2 - KM = M_2 - N$$

since

$$KM = N$$

Therefore

$$\frac{M_1 - N}{M_2 - N} = \frac{g_1}{g_2}$$

or

$$M_1 = N + (M_2 - N)\frac{g_1}{g_2}$$

Appendix F

Total loss = constant loss + variable loss

or $L_1 = L_0 + CM_1^2\theta_1$
and $L_2 = L_0 + CM_2^2\theta_2$

then

$$L_2 - L_1 = C(M_2^2\theta_2 - M_1^2\theta_1)$$

where C = constant, from which

$$C = \frac{L_2 - L_1}{M_2^2\theta_2 - M_1^2\theta_1}$$

and

$$L_0 = L_2 - M_2^2\theta_2\left(\frac{L_2 - L_1}{M_2^2\theta_2 - M_1^2\theta_1}\right)$$

or

$$L_0 = L_2 - \frac{L_2 - L_1}{1 - \left(\frac{\theta_1}{\theta_2}\right)\left(\frac{M_1}{M_2}\right)^2}$$

Appendix G

Referring to Figures 11 and 12, it is seen that the load factor varies from a value of 1.0 at the base load to a value of ϕ_0 at the maximum peak and output for a given time, the line OC being the locus of the peaks for unity load factor. The curve defining this variation may be expressed by

$$(1-\phi) = C(W - K_0M_0T)^\beta$$

where C = constant, when

$$\phi = \phi_0, \quad W = \phi_0M_0T$$

so that

$$(1-\phi_0) = C(\phi_0M_0T - K_0M_0T)^\beta$$

from which

$$C = \frac{(1-\phi_0)}{(\phi_0M_0T - K_0M_0T)^\beta}$$

Therefore

$$(1-\phi) = (1-\phi_0)\left[\frac{W - K_0M_0T}{\phi_0M_0T - K_0M_0T}\right]^\beta$$

$$= (1-\phi_0)\left[\frac{\frac{W}{M_0T} - K_0}{\phi_0 - K_0}\right]^\beta$$

and

$$\phi = 1 - (1-\phi_0)\left[\frac{\frac{W}{M_0T} - K_0}{\phi_0 - K_0}\right]^\beta$$

Appendix H

Referring to Figure 14, it is assumed that the average load for one of Q units is the same as the average value for a single unit. If the average load be taken as unity, the maximum load for a single unit becomes $1/\phi_1$ and for each of Q units, $1/\phi_Q$. Some curve connecting A and B represents the variation of $1/\phi$ with Q.
When $Q \overset{\rightarrow}{=} \infty$, ϕ and $1/\phi \overset{\rightarrow}{=} 1$, the average.
When $Q=1$, $\phi=\phi_1$ and $1/\phi_1$ is the maximum for 1 unit.

Curve A-B may be expressed in the hyperbolic form

$$\left(\frac{1}{\phi} - 1\right)^\alpha \times Q = C \tag{52}$$

where C = constant. When

$$\phi = \phi_1 \quad Q = 1$$

and

$$C = \left(\frac{1}{\phi_1} - 1\right)^\alpha$$

When

$$\phi = \phi_Q \quad Q = Q$$

and

$$C = \left(\frac{1}{\phi_Q} - 1\right)^\alpha \times Q$$

then

$$\left(\frac{1}{\phi_1} - 1\right)^\alpha = \left(\frac{1}{\phi_Q} - 1\right)^\alpha Q \tag{53}$$

and

$$\left(\frac{1}{\phi_Q} - 1\right) = Q^{-1/\alpha}\left(\frac{1}{\phi_1} - 1\right) \tag{54}$$

or

$$\frac{1}{\phi_Q} = Q^{-1/\alpha}\left(\frac{1}{\phi_1} - 1\right) + 1 \tag{55}$$

so that

$$\phi_Q = \frac{1}{Q^{-1/\alpha}\left(\frac{1}{\phi_1} - 1\right) + 1} \tag{56}$$

For the special case when the minimum load is zero, so that $N = KM = O$, equation 56 becomes

$$\phi_Q = \frac{1}{Q^{-1/\psi}\left(\frac{1}{\phi_1} - 1\right) + 1} \tag{57}$$

also

$$\theta = \frac{\phi}{2-\phi} \quad \text{or} \quad \phi = \frac{2\theta}{1+\theta}$$

from appendix B
substituting in equation 57

$$\frac{2\theta_Q}{1+\theta_Q} = \frac{1}{Q^{-1/\psi}\left(\frac{1-\theta_1}{2\theta_1}\right) + 1} \tag{58}$$

from which

$$\theta_Q = \frac{1}{Q^{-1/\psi}\left(\frac{1}{\theta_1} - 1\right) + 1} \tag{59}$$

Appendix I

Equipment I²R Losses and Capacities

1. A 400-ton suburban train is operating on a schedule for which the average current in the traction motors is 20 per cent of the maximum. To find a figure for the increase in copper losses in the motors if trailers be added to make a total train-weight of 600 tons:

(*a*) The accelerating relays and starting-resistance grids are not altered, and the same schedule is kept by less coasting.

(*b*) The relays are altered to permit 25 per cent more accelerating current.

(*a*) Let

W_1 = energy consumption of motors for 400-ton train
W_2 = energy consumption of motors for 600-ton train

Then

$$W_1 = \phi_1M_1T_1; \quad W_2 = \phi_2M_2T_2$$

and

$$\frac{W_2}{W_1} = \frac{\phi_2}{\phi_1}$$

Since

$M_1 = M_2$ and $T_1 = T_2$

Since

$W_2 = 1.5W_1 \quad \phi_2 = 1.5\phi_1$
$\phi_1 = 0.2$

Therefore

$\phi_2 = 0.3$

From equation 6

$$\theta_1 = \frac{\phi_1}{2-\phi_1} = \frac{0.2}{1.8} = 0.111$$

$$\theta_2 = \frac{\phi_2}{2-\phi_2} = \frac{0.3}{1.7} = 0.176$$

Per cent increase in copper losses

$$= \frac{\theta_2 - \theta_1}{\theta_1} \times 100 = \frac{0.176 - 0.111}{0.111} \times 100$$

= 58.6 per cent. Say 59 per cent.

(*b*) $W_1=\phi_1M_1T_1$ and $W_2=\phi_2M_2T_2$
But $W_2=1.5W_1$; $M_2=1.25M_1$ and since $T_1=T_2$
Therefore

$$W_2=\phi_2M_2T_2=1.5W_1=1.5\phi_1M_1T_1$$

or

$$\phi_2\times1.25M_1T_2=1.5\phi_1M_1T_1$$

or

$$\phi_2=\frac{1.5\phi_1}{1.25}=\frac{1.5\times0.2}{1.25}=0.24$$

Then

$$\theta_1=\frac{\phi_1}{2-\phi_1}=\frac{0.2}{1.8}=0.111$$

and

$$\theta_2=\frac{\phi_2}{2-\phi_2}=\frac{0.24}{1.76}=0.136$$

Let L_1 = present copper loss
L_2 = new copper loss

Then

$$\frac{L_2}{L_1}=\frac{C\times0.136\times(M_2)^2}{C\times0.111\times(M_1)^2}=\frac{0.136}{0.111}(1.25)^2=1.92$$

where C is a constant
or $L_2=1.918L_1$
Per cent increase in copper loss is 92 per cent.

2. An induction motor is to be applied to a load which is expected to fluctuate between no load and 500 horsepower momentarily, and it is known that 60 horsepower-hours must be delivered at the shaft during a 20-minute cycle. To determine the minimum necessary rating of this motor:
Given:

$$M=500\text{ hp};\ W=60\text{ hp-hr};\ T={}^1/_3\text{ hr};\ K=0$$

Then

$$A=\frac{W}{T}=60\div{}^1/_3=180\text{ hp}$$

and

$$\phi=\frac{A}{M}=\frac{180}{500}=0.36$$

From equation 7

$$\rho=\frac{1}{\phi(2-\phi)}=\frac{1}{0.36\times1.64}=1.694$$

By definition $r=uA$ and $r^2=\rho A^2$ or

$$r=A\sqrt{\rho}=180\sqrt{1.694}=234\text{ horsepower}$$

which is the required rating. The next higher standard rating is 250 horsepower, and a motor of this capacity will serve, provided it has sufficient torque to carry the 500-horsepower peak.

3. A motor-generator set supplying a reversing rolling-mill motor consumes 150,000 kilowatt-hours during a 24-hour run. Switchboard instruments show that the power to this set fluctuates between 400 and 12,000 kw with almost constant power factor. The cable system to the motor is known to have a loss of 50 kw on a 3,000-kw load. It is required to find the average loss in the cables, and the transformer capacity needed for this load only, if a separate bank be installed to carry this set and cable losses. Power factor 0.83. Given:

$$W=150{,}000\text{ kwhr};\ M=12{,}000\text{ kw};\ KM=400\text{ kw};\ T=24\text{ hr}$$

$$A=\frac{150{,}000}{24}=6{,}250\text{ kw}\quad \phi=\frac{6{,}250}{12{,}000}=0.5208$$

$$K=\frac{400}{12{,}000}=0.0333$$

Cable Losses

At maximum load $=50\times\left(\frac{12{,}000}{3{,}000}\right)^2=800$ kw

At minimum load $=50\times\left(\frac{400}{3{,}000}\right)^2=0.89$ kw

say 1.0 kw

Average cable losses $=\theta\times$loss at maximum load $=\theta\times800$

From equation 5

$$\theta=\phi^2+\frac{(\phi-K)^2\times(1-\phi)^2}{(1-K)^2-(1-\phi)^2}$$
$$=(0.521)^2+\frac{(0.521-0.033)^2(1-0.521)^2}{(1-0.033)^2-(1-0.521)^2}$$
$$=0.350$$

therefore average cable losses $=0.350\times800=280$ kw

For Transformer

Maximum load = 12,000+800 = 12,800 kw
Average load = 6,250+280 = 6,530 kw
Minimum load = 400+ 1 = 401 kw

Then

$$\phi=\frac{6{,}530}{12{,}800}=0.510$$

$$K=\frac{401}{12{,}800}=0.031$$

and

$$\theta=\phi^2+\frac{(\phi-K)^2\times(1-\phi)^2}{(1-K)^2-(1-\phi)^2}$$
$$=(0.510)^2+\frac{(0.510-0.031)^2(1-0.510)^2}{(1-0.031)^2-(1-0.510)^2}$$
$$=0.339$$

$$r^2=M^2\theta\text{ or }r=M\sqrt{\theta}=12{,}800\sqrt{0.339}$$
$$=7{,}450\text{ kw}$$

and at 83 per cent power factor the transformer capacity required = 8,980 kva, say 9,000 kva.

Extrapolation of Demands

4. On one of the days of maximum load a utility company had a peak load of 245,000 kw and an output of 3,116,400 kilowatt-hours. On a day of less load the output was 2,508,000 kilowatt-hours with a peak of 190,000 kw. With this basis, to determine the peak which may be expected when the daily output reaches 3,500,000 kw, the base load is 35,000 kw.
Given:

$M_0=245{,}000$ kw $\quad W_0=3{,}116{,}400$ kwhr
$M_1=190{,}000$ kw $\quad W_1=2{,}508{,}000$ kwhr

$$K_0M_0=35{,}000\text{ kw}$$

or

$$K_0=\frac{35{,}000}{245{,}000}=0.143$$

$$T=24\text{ hr}\quad W_2=3{,}500{,}000\text{ kwhr}$$

$$\phi_0=\frac{W_0}{M_0T}=\frac{3{,}116{,}400}{245{,}000\times24}=0.53$$

$$\phi_1=\frac{W_1}{M_1T}=\frac{2{,}508{,}000}{190{,}000\times24}=0.55$$

From equation 14

$$\phi_1=1-(1-\phi_0)\left[\frac{\frac{W_1}{M_0T}-K_0}{\phi_0-K_0}\right]^{\beta}$$

or

$$0.55=1-(1-0.53)\left[\frac{\frac{2{,}508{,}000}{245{,}000\times24}-0.143}{0.53-0.143}\right]^{\beta}$$

from which $\beta=0.140$

Therefore

$$\phi_2=1-(1-0.53)\times\left[\frac{\frac{3{,}500{,}000}{245{,}000\times24}-0.143}{0.53-0.143}\right]^{0.140}=0.52$$

$$M_2=\frac{W_2}{\phi_2T}=\frac{3{,}500{,}000}{0.52\times24}=280{,}000\text{ kw}$$

5. To find a value for the increase in demand in an apartment house which may be caused by the installation of 125 electric refrigerators and 125 electric ranges. Each refrigerator is reported to consume 240 watts and about 40 kilowatt-hours monthly. Each range has an average peak of 3,500 watts and consumes about 125 kilowatt-hours monthly.

For the Refrigerators

$M_1=240$ watts $\quad T=24\times30=720$ hrs per month
$W_1=40{,}000$ watt-hours $\quad Q=125$

$$A_1=\frac{40{,}000}{720}=56\text{ watts}\quad \phi_1=\frac{A_1}{M_1}=\frac{56}{240}=0.23$$

From equation 14

$$\phi_Q=\frac{1}{Q^{-1/\alpha}\left(\frac{1}{\phi_1}-1\right)+1}$$
$$=\frac{1}{\frac{\left(\frac{1}{0.231}-1\right)}{\sqrt[3]{125}}+1}=0.60$$

where α assumed to have a value of 3.0.

Therefore

maximum coincident demand (M_{Q1}) =

$$\frac{56\times125}{0.60}=11{,}700 \text{ watts, say 12 kw}$$

and

$$\text{demand factor}=\frac{11{,}700}{125\times240}=0.39$$

For the Ranges

$M_2=3{,}500$ watts $T=720$ hr
$W_2=125{,}000$ watt-hours $Q=125$

$$A_2=\frac{125{,}000}{720}=174\text{watts}\quad \phi_2=\frac{174}{3{,}500}=0.050$$

$$\phi_Q=\frac{1}{\dfrac{\left(\dfrac{1}{0.05}-1\right)}{\sqrt[3]{125}}+1}=0.21$$

therefore

maximum coincident demand (M_{Q2}) =

$$\frac{174\times125}{0.21}=103{,}600 \text{ watts, say 104 kw}$$

and

$$\text{demand factor}=\frac{103{,}600}{125\times3{,}500}=0.24$$

Since the two peaks can be coincident the combined maximum demand is 104+12 = 116 kw, and the over-all demand factor =

$$\frac{116}{125(0.24+3.5)}=0.25$$

6. Measurements taken at a transformer bank which supplies a group of 300 residences show a peak of 130 kw and an average load of 24 kw. The maximum consumption rate in these homes is known to be about one kilowatt each. To determine the expected demand in a similar group of 450 houses; also for one of 1,000 houses.

For the Group of 450 Houses

Given for 300 houses

$$M_1=1\text{ kw}\quad A_1=\frac{24}{300}=0.08\text{ kw}$$

$$\phi_1=\frac{0.08}{1}=0.08$$

$$M_Q=130\text{ kw}\quad A_Q=24\text{ kw}\quad \phi_Q=\frac{24}{130}=0.185$$

$$Q=300\quad \text{Demand factor}=\frac{130}{300\times1}=0.433$$

From equation 14

$$\phi_Q=\frac{1}{Q^{-1/\alpha}\left(\dfrac{1}{\phi_1}-1\right)+1}$$

$$=\frac{1}{300^{-1/\alpha}\left(\dfrac{1}{0.08}-1\right)+1}=0.185$$

from which $\alpha=5.96$. For the 450 houses $Q=450$ and

$$\phi_{Q1}=\frac{1}{(450)^{-\frac{1}{5.96}}\left(\dfrac{1}{0.08}-1\right)+1}=0.195$$

and maximum demand for 450 houses =

$$\frac{24}{0.195}\times\left(\frac{450}{300}\right)=185\text{ kw}$$

$$\text{demand factor}=\frac{185}{450\times1}=0.41$$

For 1,000 houses

$$\phi_Q=\frac{1}{(1{,}000)^{-\frac{1}{5.96}}\left(\dfrac{1}{0.08}-1\right)+1}=0.217$$

maximum demand for 1,000 houses =

$$\frac{24}{0.217}\times\left(\frac{1{,}000}{300}\right)=369\text{ kw}$$

and

$$\text{demand factor}=\frac{369}{1{,}000\times1}=0.37$$

Distribution and Transmission Losses

7. During two periods within a year, for 31 days and 59 days respectively, the records of a utility company show the results given in Table A.

Required to find the constant losses of the distribution system:

Table A

Generated Kwhr	Sold Kwhr	1-Hr Peak, Kw	Minimum Load Kw
86,421,000	72,080,000	242,000	41,100
146,566,000	122,498,000	195,300	35,000

8. A projected transmission line is to deliver 100 million kilowatt-hours annually with a line loss not to exceed five million kilowatt-hours. There will be a base load of 5,000 kw and a peak of 25,000 kw. The permissible line loss at the maximum load is required.

$M=25{,}000$ kw $KM=5{,}000$ kw
$W=100{,}000{,}000$ kwhr $L=5{,}000{,}000$ kwhr
$T=8{,}760$ hr

$$A=\frac{100{,}000{,}000}{8{,}760}=11{,}420\text{ kw}$$

$$\phi=\frac{11{,}420}{250{,}000}=0.457$$

$$K=\frac{5{,}000}{25{,}000}=0.2$$

Given:
$M_1=242{,}000$ kw
$T_1=31$ days
$K_1M_1=41{,}000$ kw
$W_1=86{,}421{,}000$ kwhr

$M_2=195{,}300$ kw
$T_2=59$ days
$K_2M_2=35{,}000$ kw
$W_2=146{,}566{,}000$ kwhr

Then

$$A_1=\frac{86{,}421{,}000}{31\times24}=116{,}200\text{ kw}\qquad A_2=\frac{146{,}566{,}000}{59\times24}=103{,}500\text{ kw}$$

$$\phi_1=\frac{116{,}200}{242{,}000}=0.48\qquad \phi_2=\frac{103{,}500}{195{,}300}=0.53$$

$$L_1=\frac{86{,}421{,}000-72{,}080{,}000}{31\times24}=19{,}300\text{ kw}\qquad L_2=\frac{146{,}566{,}000-122{,}498{,}000}{59\times24}=17{,}000\text{ kw}$$

$$K_1=\frac{41{,}100}{242{,}000}=0.17\qquad K_2=\frac{35{,}000}{195{,}300}=0.18$$

From equation 9

$$\theta=\phi^2+0.273(\phi-K)^2$$

Then

$$\theta_1=(0.48)^2+0.273(0.31)^2=0.257$$
$$\theta_2=(0.53)^2+0.273(0.35)^2=0.314$$

From equation 13

$$L_0=L_2-\frac{L_2-L_1}{1-\dfrac{\theta_1}{\theta_2}\left(\dfrac{M_1}{M_2}\right)^2}=17{,}000-\frac{17{,}000-19{,}300}{1-\dfrac{0.257}{0.314}\left(\dfrac{242{,}000}{195{,}300}\right)^2}=8{,}100\text{ kw}$$

Table B

Company	Peak Load Kw (M_1)	Duration of Peak (Minutes) (p/T × 1,440)	Production Kwhr (Thousands) (W)
A	310,000	60	1,414.828
B	148,200	15	630,941
C	407,500	20	2,152,529
D	204,000	30	1,022.187
E	55,300	5	221,384

Company	ϕ	K	KM_1	M_1-KM_1	g_2/g_1	$M_s=KM_1+(M_1-KM_1)g_1/g_2$
A	0.521	0.208	64,600	245,400	1/0.924	330,200
B	0.486	0.194	28,800	119,400	1/0.935	140,400
C	0.603	0.241	98,300	309,200	1/0.959	394,800
D	0.572	0.229	46,700	157,300	1/1.000	204,000
E	0.457	0.183	10,100	45,200	1/0.852	48,600

K assumed as 0.4ϕ $\phi=W/8{,}760M_1$ g_2/g_1 from Table II

From equation 9

$$\theta=\phi^2+0.273(\phi-K)^2=(0.457)^2+0.273)\times(0.457-0.2)^2=0.227$$

$$\text{Average loss}=\frac{5{,}000{,}000}{8{,}760}=571\text{ kw}$$

$$\text{Loss at maximum load}=\frac{\text{average loss}}{\theta}$$

$$=\frac{571}{0.227}=2{,}520\text{ kw}$$

9. An isolated feeder circuit supplies a manufacturing plant 40 million kilowatt-hours annually, metered at the plant. On the maximum day the peak was 12,000 kva at 0.85 power factor; consumption 123,000 kilowatt-hours; base load 1,800 kva. On the minimum day the peak was 9,000 kva at 0.90 power factor; consumption 105,000 kilowatt-hours with a base load of 1,500 kva.

At a load of 3,000 kva, the feeder loss is 120 kw. The annual feeder loss is required to determine if additional copper is warranted.

For the above equation, K is obtained by the ratio of W_1 to W_2 or

$$K=\frac{105{,}000}{123{,}000}=0.8537$$

The average daily energy consumption is

$$W_a=\frac{40{,}000{,}000}{365}=109{,}600\text{ kwhr}$$

so that

$$\phi_1=\frac{109{,}600}{123{,}000}=0.8910$$

then

$$n=\frac{1-0.8910}{0.8910-0.8537}=2.922$$

and

$$\begin{aligned}W_m&=[K+(1-K)(0.5)^{2.922}]W_2\\&=[0.8537+(0.1463\times0.1319)]123{,}000\\&=107{,}400\text{ kwhr}\end{aligned}$$

Given:

Annually	*Maximum Day*	*Minimum Day*
$W=40{,}000{,}000$ kwhr	$W_2=123{,}000$ kwhr	$W_1=105{,}000$ kwhr
	$M_2=12{,}000$ kva $=10{,}200$ kw	$M_1=9{,}000$ kva $=8{,}100$ kw
	$K_2M_2=1{,}800$ kva	$K_1M_1=1{,}500$ kva
	$K_2=\frac{1{,}800}{12{,}000}=0.15$	$K_1=\frac{1{,}500}{9{,}000}=0.167$
	$\phi_2=\frac{123{,}000}{24\times10{,}200}=0.502$	$\phi_1=\frac{105{,}000}{24\times8{,}100}=0.540$
	$(\phi_2-K_2)=(0.502-0.15)=0.352$	$(\phi_1-K_1)=(0.540-0.167)=0.373$
	$(1-K_2)=1-0.15=0.85$	$(1-K_1)=1-0.167=0.833$

Using curve of function 11 of Table I

$$\theta=\phi^2+{}^1/_{15}[3(\phi-K)^2-3(\phi-K)(1-K)+2(1-K)^2]$$

$$\theta_1=(0.540)^2+{}^1/_{15}[3(0.373)^2-3(0.373\times0.833)+2(0.833)^2]=0.349$$

$$\theta_2=(0.502)^2+{}^1/_{15}[3(0.352)^2-3(0.352\times0.85)+2(0.85)^2]=0.313$$

then

$$L_1=120\times\left(\frac{9{,}000}{3{,}000}\right)^2\times0.349=377\text{ kw}$$

$$L_2=120\times\left(\frac{12{,}000}{3{,}000}\right)\times0.313=602\text{ kw}$$

Using the conception of a solid as illustrated by Figure 7, the value of the energy (W_m) for the theoretical mid-section can be obtained by assuming that the daily energy consumption varies in accordance with formula 2 of Table I so that

$$\frac{w}{W_2}=K+(1-K)\left(\frac{t}{T}\right)^n$$

or

$$w=\left[K+(1-K)\left(\frac{t}{T}\right)^n\right]W_2$$

where $n=\frac{1-\phi}{\phi-K}$ and $w=W_m$ when $\frac{t}{T}=0.5$

By direct interpolation

$$M_m=8{,}100+[10{,}200-8{,}100]\times\frac{107{,}400-105{,}000}{123{,}000-105{,}000}=8{,}730\text{ kw}$$

and

at the average power factor of

$$\frac{0.90+0.85}{2}=0.875$$

$$M_m=\frac{8{,}730}{0.875}=10{,}000\text{ kva}$$

and

$$\phi_m=\frac{107{,}400}{24\times8{,}730}=0.513$$

Assume

$$\frac{K_m}{\phi_m}=\frac{\frac{K_1}{\phi_1}+\frac{K_2}{\phi_2}}{2}=\frac{\frac{0.167}{0.540}+\frac{0.150}{0.502}}{2}=0.304$$

so that

$$K_m=0.304\phi_m=0.304\times0.513=0.156$$

then

$$\theta_m=(0.513)^2+{}^1/_{15}[3(0.357)^2-3(0.357\times0.844)+2(0.844)^2]=0.323$$

and the loss corresponding to the mid-section load curve is

$$0.323\times120\times\left[\frac{10{,}000}{3{,}000}\right]^2=413\text{ kw}$$

The volume of the solid representing the losses, to correspond to Figure 7 may be considered as that of a prismatoid, the formula for which is

$$V=\frac{1}{6}h(A+B+4C)$$

where

V = volume
h = distance between end faces A and B
$A+B$ = area of end faces
C = area of mid-section

then

$h=365$ days
$A=602$ kw×24 hours
$B=377$ kw×24 hours
$C=413$ kw×24 hours

So that

$$\text{annual kwhr loss}=\frac{365}{6}\times24[602+377+(4\times413)]=4{,}133{,}000\text{ kwhr}$$

and

$$\text{the average loss}=\frac{4{,}133{,}000}{24\times365}=472\text{ kw}$$

Comparison of Maximum Demands

10. Utilities A to F report the peak loads and energy generated during a year shown in Table B. For comparative purposes, it is necessary to express all peaks on a 30-minute basis. See formula 12.

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Acoustics and the Quiet Train Ride

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Synopsis: This paper discusses those fundamentals of acoustics which are important in attacking practical problems in noise reduction. A description is given of the instruments suitable for measuring the physical quantities involved. The train problem is analyzed, and data taken by the author on a relatively quiet type of coach are presented. The various methods of noise control which may be applied to a coach are discussed, and data are presented indicating the possibilities of each method.

I. Some Fundamentals on Sound and Hearing

THE auditory sensation is subjectively simple, and so familiar that the average reaction to the acoustical engineer and his load of instruments is: "I know it's noisy, why measure it; I don't see why your meter readings jump around; you have measured two sounds which are certainly different to our ears, and your meter gives the same reading; the decibel is a silly unit; all I want is a few square feet of absorbing material which will soak up the sound.* The critical gentleman may certainly be forgiven, because his viewpoint is understandable. Nevertheless, there are very definite reasons for the techniques and vocabulary of acoustical analysis and for the variety of methods which may be needed to bring about the desired quiet ride.

Sound in Air

Sound, considered physically in air at a point, is a fluctuation in the barometric pressure above and below normal atmospheric pressure. It may be simply a sinusoidal pressure variation with time, or it may be of the greatest complexity. Using a 1,000-cycle-per-second pure tone the ear is found to be

(*a*) Sensitive—it can hear as sound a pressure variation of 2.96×10^{-9} pounds per square inch.

(*b*) Rugged—it can give a hearing sensation (with some discomfort) for a pressure variation of 9.35×10^{-2} pounds per square inch.

These are rms values. This is a remarkable range, covering a pressure ratio of 3.16×10^7 to 1. The pressure variation is usually spoken of simply as the sound pressure. The threshold of hearing varies with frequency, the ear being less acute at each end of its range. Below 20 cycles and above 15,000 cycles, the hearing sensation is not stimulated no matter how large the sound pressure.

The ear can tell us something about pressure and frequency. It has remarkable powers in judging the quality of complex sounds. It cannot remember loudness accurately, however, for even a short time. A pressure gage is needed, with indicating or recording devices, so that a measure of the pressure variation with time, at a point, may be taken. We may wish to get the instantaneous variation over the cycle, or we may be satisfied with an rms reading. When we have these we have all the data the ear ever had, and furthermore we have everything the air can tell us at this point. What we are able to deduce from it depends on experience with a variety of sound sources, and on the relations between the physical disturbance in the air and the hearing reactions of an auditor.

Decibels

The pressure units given above are inconvenient numbers and cover so wide a range that logarithmic plots would have to be used. The concept of the decibel has been adopted, because it reduces the above pressure range to 150 steps. This is useful, because it is approximately true that 150 variations in loudness may be detected in passing through this range. The difference in decibels between two sound pressures, P_2 and P_1, P_2 being the larger, is defined as

$$\text{decibel difference} = 20 \log_{10} \frac{P_2}{P_1} \qquad (1)$$

The decibel involves a ratio always. When a train noise is said to be 80 decibels, it refers to 80 decibels above some reference pressure. This reference has now been standardized at 2.96×10^{-9} pounds per square inch rms.*

The pressure level of a sound pressure P, expressed in pounds per square inch rms is

$$20 \log_{10} \frac{P}{2.96\times10^{-9}} \text{ decibels} \qquad (2)$$

If desired, sound readings may be given in pressure units. Meters are usually calibrated to read directly in pressure level, which answers most purposes very well.

The practicing acoustical engineer finds the relation between pressure level reduction in physical units and human reaction about as follows.[2]

1 decibel—Barely detectable
5 decibels—Definite improvement, worth some expenditure
10 decibels—Striking improvement, worth considerable cost
20 decibels—Outstanding improvement
25 decibels—Almost like "off and on"

For practical purposes discussed later, decibel here can be replaced by the closely related loudness level unit, phon.

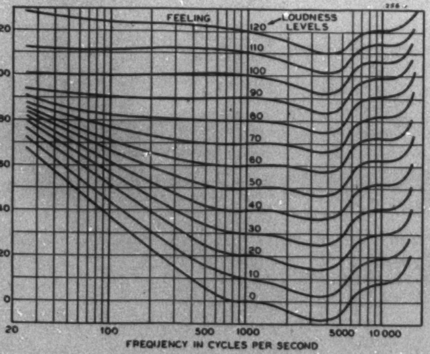

Figure 1. Loudness contours, showing how the sensation of loudness varies with frequency and pressure level

Pressure levels are in decibels; loudness levels are in phons

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*Standard nomenclature employs centimeter-gram-second units. The rms pressure at the threshold of hearing at 1,000 cycles is 0.000204 bar. See reference 1.

This table is true for initial pressure levels of 60 to 90 decibels, the range in which most noise-quieting problems lie. It is difficult to converse at 100 decibels and virtually impossible at 120 decibels. In an extremely noisy environment such as a ship engine room, acoustically untreated, a reduction from 120 decibels to 110 decibels might not be judged as "worth considerable cost." The level would still be unpleasantly high and conversation would still require great effort.

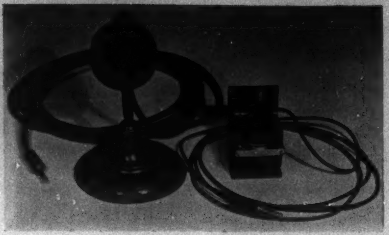


Figure 2. Typical transducers

Dynamic microphone (left) and dynamic vibration velocity unit

Vibration and Sound

In addition to the sound pressure affecting his ears, the coach passenger is subjected to direct vibration through the seat cushions, the floor, and those portions of the wall and window he may lean against. Except for outside noise entering through air leaks and inside sources such as fans, the sound pressures in the coach are built up by vibrating surfaces.

The air particles near a vibrating surface which acts as a sound source move with the same displacement and frequency as the surface and in phase with it. If the vibrating surface moves at 1,000 cycles per second with an rms velocity of 1.92×10^{-6} inches per second, the nearby sound pressure will be 2.96×10^{-9} pounds per square inch, which is the threshold of hearing. Accordingly we may write, when V is expressed as inches per second rms

Vibration velocity level =

$$20 \log_{10} \frac{V}{1.92\times10^{-6}} \text{ decibels} \qquad (3)$$

A velocity gage is needed, with indicating or recording devices so that a measure of the velocity variation with time, at a point, may be taken. We may wish to get the instantaneous variation over the cycle, or we may be satisfied with an rms reading. When we have these, we have more than the ear in air can get, because we may explore various surfaces with a probe and determine their individual vibration characteristics. The ear in air can judge merely the pressure characteristics at that point, and the pressure at that point may be dictated by several sources plus the effect of room acoustics. If desired, vibration readings may be given in velocity units. It is convenient to express them in decibels of vibration velocity level. If a surface has a vibration velocity level of 80 decibels, it will generate near it a pressure level of 80 decibels.

Displacement, Velocity, Acceleration

Just because a surface has a large displacement, it must not be thought that a loud sound will result, nor is it necessarily true that a small displacement results in a weak sound. The pressure level near the surface depends on the *velocity* of the vibration, which is a function of both displacement and frequency. Displacement and acceleration may be of interest when stresses, unbalance, metal fatigue, and passenger bodily comfort are under consideration. They may be measured by special instruments, or they may be computed from readings of vibration velocity level and frequency.

For sinusoidal vibrations we have the following relations:

$$x = B_m \sin(2\pi ft) \qquad (4)$$

$$\frac{dx}{dt} = v = B_m 2\pi f \cos(2\pi ft) = B_m 2\pi f \sin\left(2\pi ft + \frac{\pi}{2}\right) \qquad (5)$$

$$\frac{d^2x}{dt^2} = a = -B_m 4\pi^2 f^2 \sin(2\pi ft) = B_m 4\pi^2 f^2 \sin(2\pi ft + \pi) \qquad (6)$$

where

x, v, a = instantaneous values of displacement, velocity, acceleration

B_m = maximum displacement of particle from neutral position

f = frequency of vibration in cycles per second

t = time in seconds

Equation 4 describes a particle whose displacement varies sinusoidally with time, f cycles per second, between the limits $+B_m$ and $-B_m$. Let us assume $t=0$ is now. The equation shows that the particle displacement is now zero, about to increase positively. Equation 5 describes the velocity of this particle. It is similar to the displacement but differs in amplitude and phase. The equation describes a particle whose velocity varies sinusoidally with time, f cycles per second, between the limits $+B_m 2\pi f$ and $-B_m 2\pi f$. At f cycles per second, one cycle is completed in $1/f$ seconds. When $t=0$, the particle velocity is the maximum, $+B_m 2\pi f$, about to decrease. At a time $1/4f$ seconds earlier, ($t=-1/4f$), the velocity was zero about to increase positively. This is a sufficient value for t which makes $\sin(2\pi ft+\pi/2)=0$ and about to increase. This is the same state the displacement is in now. Thus, the velocity "leads" the displacement by $1/4f$ seconds, which is the time required to execute one-quarter cycle. With the convention of representing a vibration as the projection of a vector rotating in a circle, the velocity leads the displacement by 90 degrees. Equation 6 describes the acceleration of this particle. It is similar to the displacement but differs in amplitude and phase. The equation describes a particle whose acceleration varies sinusoidally with time, f cycles per second, between the limits $+B_m 4\pi^2 f^2$ and $-B_m 4\pi^2 f^2$.

Table I. RMS Displacements, Velocities, and Accelerations Over the Range of Sound Levels and Frequencies

Units Are Inches, Inches Per Second, Inches Per Second Squared

	0 Decibel			80 Decibels			150 Decibels		
Frequency	D	V	A	D	V	A	D	V	A
1	3.06×10^{-7}	1.92×10^{-6}	1.21×10^{-5}	3.06×10^{-3}	1.92×10^{-2}	1.21×10^{-1}	9.67	60.7	3.81×10^{2}
10	3.06×10^{-8}	1.92×10^{-6}	1.21×10^{-4}	3.06×10^{-4}	1.92×10^{-2}	1.21	9.67×10^{-1}	60.7	3.81×10^{3}
100	3.06×10^{-9}	1.92×10^{-6}	1.21×10^{-3}	3.06×10^{-5}	1.92×10^{-2}	1.21×10^{1}	9.67×10^{-2}	60.7	3.81×10^{4}
1,000	3.06×10^{-10}	1.92×10^{-6}	1.21×10^{-2}	3.06×10^{-6}	1.92×10^{-2}	1.21×10^{2}	9.67×10^{-3}	60.7	3.81×10^{5}
10,000	3.06×10^{-11}	1.92×10^{-6}	1.21×10^{-1}	3.06×10^{-7}	1.92×10^{-2}	1.21×10^{3}	9.67×10^{-4}	60.7	3.81×10^{6}

When $t=0$, the particle acceleration is zero about to increase negatively. At a time $1/2f$ seconds earlier ($t=-1/2f$), the acceleration was zero about to increase positively. This is a sufficient value for t which makes $\sin(2\pi ft+\pi)=0$ and about to increase. This is the same state the displacement is in now. Thus the acceleration leads the displacement by $1/2f$ seconds, which is the time required to execute one-half cycle, or the acceleration leads the displacement by 180 degrees. The phase differences are indicated directly in equations 5 and 6 as $\pi/2$ and π respectively. The explanation has been given to help visualize the actual time differences involved, and what the differences mean in terms of the complete cycle.

In Table I are given the rms displacements, velocities, and accelerations associated with 0, 80, and 150 decibels pressure levels. The units are respectively inches, inches per second, and inches per second squared. These tabulations represent physical conditions, and the decibel is a physical unit. For example, a vibration of an air particle at one cycle per second having an rms displacement of 3.06×10^{-3} inches develops an rms velocity of 1.92×10^{-2} inches per second and is thus 80 decibels pressure level. Due to the limitations of the ear, this would not evoke the sensation of hearing. However, it is perfectly possible for a vibrating surface or air particle to have this motion. This vibration in a surface could be detected by the fingers. Sound pressure levels of 150 decibels at 1,000 cycles have been measured in airplane motor test houses. Such levels are reachable by generating nonstreamline flow in air by propellor blades or causing high-speed jets to impinge on sharp edges. They are not reachable generally by flat surfaces vibrating normal to their plane. Note that the rms acceleration associated with this level is 3.81×10^{5} inches per second squared. This is approximately 1,000 times gravity ($a_g=386$ inches per second squared). A large force would be required to move even a lightweight panel at this acceleration, and the mass reactions throughout the panel would be so high the inherent stiffness would not be great enough to make the panel vibrate as a plane.

So far we have discussed mainly a physical way of designating sounds and vibrations. Ear sensations have been referred to in terms of the threshold of hearing at 1,000 cycles, the region of pain at 150 decibels, and the reaction of an auditor to various decibel steps in noise reduction. We will now discuss loudness level and its relation to pressure level for pure tones.

Pressure Level and Loudness Level

The loudness level of a pure tone is defined as the pressure level of the equally loud 1,000-cycle tone, a matter decided by the vote of a sound jury.[1] The unit is the phon. Figure 1 shows this relationship in the form of equal loudness contours. The loudness level in phons of a 1,000-cycle tone is the same as the pressure level in decibels. For any other frequency the loudness level varies with the pressure level, varying both with frequency and with pressure level. In general, the ear lacks acuity at low frequencies and low pressure levels. Typical values are given in Table II. The tabulated values are decibels pressure level. Using the "70 phons" column for example, the table shows that 82 decibels are required at 50 cycles to evoke the hearing sensation of 70 phons loudness level, but only 67 decibels are required at 3,000 cycles to evoke the sensation of 70 phons. The "0 phons" column gives the minimum pressure levels in decibels necessary to evoke the sensation of hearing. For example, the ear threshold is 53 decibels higher at 50 cycles and 36 decibels higher at 100 cycles than it is at 1,000 cycles. At 3,500 cycles the ear is most acute. At high levels the ear has a substantially flat response, as shown by the "100 phons" column. The response at intermediate levels is indicated by the "40 phons" and "70 phons" columns.

Adding Decibels

If the measured sound pressure in air at a point is P_1, the energy E_1 flowing through a unit area in unit time is given by

$$E_1=kP_1^2 \tag{7}$$

where k is a constant of proportionality depending on the system of units selected. Accordingly, from equations 2 and 7, letting P_0 represent the threshold pressure, and E_0 the threshold energy

$$\text{Pressure level}=20\log_{10}\frac{P_1}{P_0}=10\log_{10}\frac{P_1^2}{P_0^2}=10\log_{10}\frac{E_1}{E_0} \tag{8}$$

Suppose that a single source of sound causes a measured pressure level L_1. The energy associated with it is E_1. Suppose that we stop the first source and sound

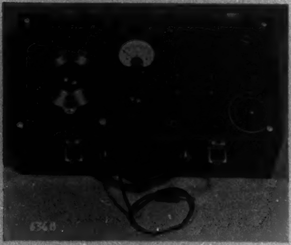


Figure 3. One type of electrical filter

Heterodyne type passing a narrow band of frequencies

Table II. Pressure Levels in Decibels Required Over the Frequency Range to Give Four Loudness Levels

	Loudness Levels—Phons			
Frequency	0	40	70	100
50	53	72	82	100
100	36	62	78	100
250	18	49	72	100
500	6	42	70	101
1,000	0	40	70	100
1,500	−2	40	70	98
2,000	−4	39	69	97
2,500	−6	38	68	95
3,000	−7	37	67	93
3,500	−8	37	67	92
4,000	−7	38	67	92
5,000	−5	39	69	95
7,500	6	51	81	105
10,000	8	52	82	105

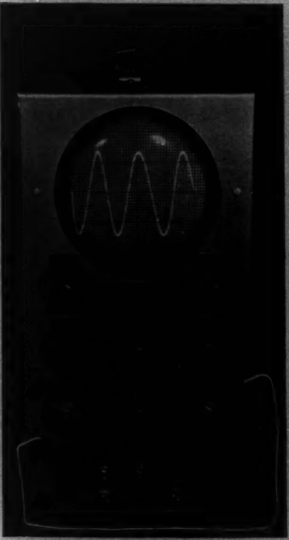


Figure 4. Oscilloscope

Used in study of wave form of original sound, indicating instantaneous variations over the cycle a second source, which causes a measured pressure level L_2, with an associated energy E_2. If now we sound both sources together, the energy passing through the point of measurement will be E_1+E_2. For the random phase relationships which occur with multiple wall reflections and radiation from complex sources, the new pressure will be $\sqrt{P_1^2+P_2^2}$, where P_1 and P_2 are the pressures associated with L_1 and L_2. Using equation 8 and the fact that associated energies add arithmetically, it may be shown that if Δ is the difference in decibels between L_1 and L_2, L_1 being the larger, the new pressure level is obtained by adding β to L_1 as shown in Table III.

Handling the Analysis

Suppose that an analysis of pressure level under representative conditions has been made. The pressure levels measured are given in Table IV, column 1. In column 2 are the loudness levels from these, obtained from the loudness contours of Figure 1. Single frequencies near the mid-points of the bands were used to represent the bands: 30, 150, 300, 600, 1,200, 2,400, 3,600, 6,000 cycles.

The "total" figures for the decibel values of columns 1 and 4 could have been obtained by computing the energies in the bands, adding the individual energies to get the total energy, then converting back to pressure level. Actually the short-cut method of Table III was used, which is entirely satisfactory. Strictly speaking, individual band loudness levels in phons do not add as decibels do, but the author has found that a total figure obtained this way is a practical method of assigning a single number having some meaning in terms of the sensation of hearing. This is not the preferred method. Noise-reduction problems should be judged by the reduction in each band. This is discussed at greater length under "multiple sources."

Suppose that an outstanding improvement is desired in the total loudness level. By our standard, this calls for a 20-phon reduction from 88 to 68 phons. Most commercial band spectra give a total that is 2–5 decibels (or phons) higher than the highest single component. In this case, we might guess at a design objective of 64 phons for the loudest band after acoustical correction, with the other bands reduced to something under 64 phons, and write down column 3. This desired spectra has been made to drop off in the high frequency end, as experience has shown the high frequencies have a heavy "annoyance weighting." The total turns out to be 69 phons or a reduction of 19 phons. We will let this trial stand. In column 4 the phons loudness level of column 3 have been converted to decibels pressure level, using the loudness contours of Figure 1. Column 5 shows the decibel reductions necessary in each band to produce the desired loudness level projected in each band. It may prove impossible to effect an 11-decibel reduction for example, in the 50–100 band within the price, weight, and space limitations imposed upon train problems, but by this type of analysis a rational method is available for surveying and attacking a complicated situation.

Table III. Addition Factor for Combining Two Decibel Levels Knowing Their Difference

Difference in Two Level—Δ	Add to Larger Level—β
0	3.0
1	2.5
2	2.1
3	1.8
4	1.5
5	1.2
6	1.0
7	0.8
8	0.6
9	0.5
10	0.4

Multiple Sources

Table IV is a general procedure based on measuring the interior level. It is usually necessary to investigate further a complex structure such as a train to determine the contribution of each source to the interior pressure level. Sometimes controlled experiments can be run, such as driving the compressors with the train stationary, or checking the floor transmission by an artificial sound source. More often the interior must be probed for vibration velocity level and an estimate made as to why certain panels vibrate and what can be done to reduce their contributions to interior pressure level.

Table IV. Handling a Pressure Level Analysis (See Text)

Frequency Bands	Measured Db Pressure Level Col. 1.	Measured Phons Loudness Level Col. 2	Design Phons Loudness Level Col. 3	Design Db Pressure Level Col. 4	Decibel Reduction Necessary Col. 5
0–50	91	83	63	85	6
50–100	87	84	64	76	11
100–200	81	79	62	69	12
200–400	78	77	58	61	17
400–800	75	75	55	55	20
800–1,600	72	72	52	52	20
1,600–3,200	65	66	48	46	19
2,400–4,800	60	62	43	41	19
4,800+	48	43	38	43	5
Total	93	88	69	86	

Suppose that near a certain passenger position a complete survey of likely sources is made. Hypothetical data for the 400–800-cycle band are presented in Table V. From this tabulation we can make certain informed guesses.

The noise from the air conditioner appears to come directly through the air to the passenger, as no panels have been discovered which can account for 50 decibels. The noise when "at speed" appears to be due to (*a*) sound pressures generated at the wheels striking the floor and coming through in part, or (*b*) broadcasting of sound energy from the floor surface, or both. The floor may be driven strongly from the frame, which is subjected to road shocks. Table V does not tell us whether the other panels vibrate at the levels shown, because they are part of the strongly vibrating floor system, or because external sound pressures cause them to vibrate at these levels.

The data suggest that measurement of external levels under the coach and at window height would throw further light on the mechanism of sound transfer. As the air-borne pressure level due to the compressor is 60 decibels, reducing the "at-speed" floor contribution much below 57 decibels is a waste of time and money, unless the compressor is also reduced. This is a direct consequence of the decibel additive relationship shown in Table III. With 60 decibels and 57 decibels, the total is 61.8 decibels. If the floor contribution were removed entirely, the pressure level could drop only 1.8 decibels which is not an important amount.

The noise from the compressor appears to come through the floor either by its air-borne noise striking the underside of the floor or by structural vibration or both. With a tube-like structure such as a coach, an intense source in one portion, such as vestibule noise leaking through a

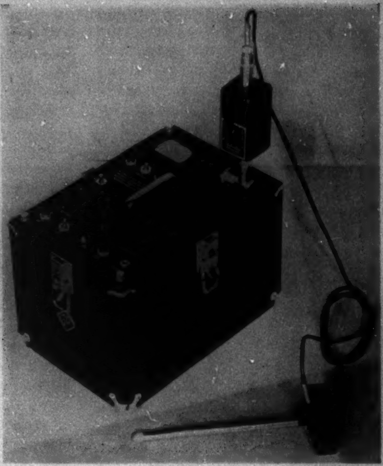
Table V. Investigation of Probable Sources (See Text)

	Train Stationary		Train at Speed
	Compressor Only	Air Conditioner Only	Compressor and Conditioner Operating
Air-borne pressure level	60	50	75
Floor vibration velocity level	63	35	79
Window vibration velocity level	57	32	70
Side panel vibration velocity level	55	30	65
Ceiling vibration velocity level	50	32	62

Figure 5. High-speed level recorder

Makes graphic record which can be read as pressure level against time; an rms type of instrument

Figure 6. Sound-level meter with crystal vibration pickup



partly open door, will be distributed as air-borne sound. This type of trouble is readily detected, however, by exploring likely panels and air spaces for intense local sources.

A most practical and important aspect of the decibel additive relationship of Table III occurs when the contributions of each frequency band to the total are considered. Usually the total level is dictated by the most intense band, which in transportation is in the low frequency end. If all the energy above 500 cycles were removed, the *total* level would not decrease by an amount sufficient to measure. The ear would notice a big difference, and it has been the author's experience[3] that reductions in the higher frequencies are useful out of all proportion to their contributions to total level. The ear will readily detect the difference in a coach when hisses, rattles, squeaks and high-pitched whines have been eliminated. It will judge this coach definitely quieter than before, even though the low-frequency components which dictate total level have not been reduced. A band analysis will show the higher frequencies have been reduced substantially, and this portion of the measurement will be in good agreement with the ear judgment.

Sound in a Closed Space

The pressure level built up inside a closed space by air-borne sound striking the outside may be computed closely enough for practical purposes by a formula derived from a simplified mathematical analysis.

$$PL_i = PL_o - TL - 10 \log_{10} \frac{a}{A} \qquad (9)$$

where

PL_i = average inside pressure level
PL_o = pressure level of the sound outside striking the transmitting area A
TL = transmission loss of the area A
a = total sabines absorption inside the closed space
A = transmitting area in square feet

Transmission loss is the decibel drop produced on air-borne sound by a sound barrier, measured close to each side, under conditions that discount the effect of reflected waves. The sabines absorption of a surface is the product of its area in square feet by its coefficient of sound absorption. The coefficient may be thought of as the percentage of energy absorbed at one incidence when a ray of sound strikes the surface at a random angle.

If a closed space has few sabines initially, PL_i can be reduced appreciably by adding an efficient acoustical treatment. Generally speaking, more improvement is possible in working with TL alone than in working with acoustical treatment alone for sources outside the space. Adding a treatment is discussed further in section III.

II. Electrical Instruments Suitable for Noise-Reduction Problems

We have seen that

(*a*) The sound pressures and vibration velocities of interest to the acoustical engineer are quantities covering an enormous range but, even at their largest, small by ordinary standards.

(*b*) The ear responds in a complex manner depending on both the frequency and intensity of the sound in question.

(*c*) It is important to measure the changes in each frequency band.

Rugged instruments of adequate analyzing power are required.

It is safe to say that the developments of the vacuum-tube amplifier and the high-quality microphone have allowed the engineering practice of acoustics to reach its present stage. Because proper electrical instruments are available, field problems can be analyzed in a rational manner, numbers can be assigned to physical measurements which have meaning in terms of human ear reactions, and corrective measures, developed by the experimental method in the laboratory, can be applied to the field problems.

Figure 7. Complete analyzing equipment

Makes graphic record which can be read as pressure level against frequency. Analyzer passes a narrow band of frequencies, covering audible range automatically

The displacements associated with frequencies below 20 cycles are sometimes large. Because of this, the curve of displacement against time often can be traced by the seismic type of instrument with mechanical leverage to obtain desired amplification. Optical magnification types are also available. This region is of interest in investigations of riding comfort. See, for example, the work by Jacklin[4] and his associates. The problems of measuring and correcting unbalance in rotating machinery have been the subject of extensive investigations outside the scope of this paper. See, for example, publications by Thearle[5] and Rushing.[6]

The acoustical engineer in the wide regions of the audible spectrum finds indispensable the electrical method of measurement, because frequency analysis is desirable, and the displacements associated with his problems are very small indeed. In general, the instrument assembly consists of four parts:

Transducer.
Amplifier.
Filter.
Final indicating or recording means.

A transducer is a device which has an electrical output similar to the mechanical input to the device. The output energy can be amplified by the desired amount necessary to give the final reading. Filter networks which will give a frequency discrimination can be inserted ahead of the final reading, enabling the frequencies involved in the original vibration to be investigated.

What follows is not intended to be an exhaustive catalogue of sound measuring equipment. It does describe in general terms the types which the author has found useful in the field and laboratory.

(1) Transducers

(*a*) MICROPHONES

1. *Dynamic.* A diaphragm exposed to sound pressure carries a coil moving in a permanent magnetic field. A preferred type is resistance controlled, having a substantially flat response over the frequency range. The velocity of the diaphragm, and hence the voltage output of the coil, is proportional to the sound pressure.

2. *Condenser.* This microphone consists in its elements of two plates close together which form a condenser in the amplifying circuit. Sound pressure moves one of the plates, causing a voltage fluctuation. The natural frequency is high, and the damping and polarizing voltages are such that the response is substantially flat. This type usually requires a built-in stage of amplification which may be inconvenient in the field.

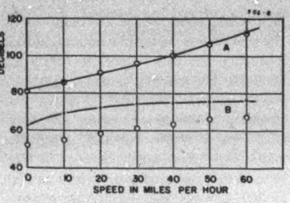


Figure 8. Train noise

Graph of total pressure level versus speed
Curve *A*—Noise underneath
Curve *B*—Noise inside at passenger head level
Circles—Associated loudness levels

3. *Ribbon.* A thin metallic ribbon exposed to sound pressure moves in a permanent magnetic field. With the back of the ribbon enclosed, this microphone responds to the sound pressure on the exposed face and is nondirectional. With both sides of the ribbon open to the sound field, the microphone responds to the pressure gradient and is insensitive in the plane of the ribbon. This is often called a velocity microphone. The two types are sometimes combined into one microphone having a substantially unidirectional response.

4. *Crystal.* A crystal of Rochelle salt exhibits the piezoelectric effect, generating an electromotive force when deformed. Sound pressures cause this deformation and the microphone using this principle has a substantially flat response.

(*b*) VIBRATION PICKUPS

These instruments are, in general, similar to microphones but are adapted to measure directly vibration in a mechanical part, rather than to respond to the sound pressure built up by moving air particles. The self-contained dynamic type carries a stylus fastened to a coil moving in a permanent magnetic field. The stylus is positioned against the vibrating member and partakes of its motion exactly. The case of the instrument, due to its inertia and weak coupling spring, remains at rest in space. Voltage output is proportional to coil velocity and, thus, to the velocity of the investigated part. For laboratory work a coil may be fastened to the vibrating part and made to move in a magnetic field, or a plate forming part of a condenser may be attached. A crystal pickup is available which is light (two ounces) and useful where the heavier dynamic type would interfere with the free motion of the part. The crystal may be mounted with a free corner, and the case shaken by the vibrating part. The output of this crystal type increases with frequency, for constant velocities of vibration. This may be useful where increased weighting should be given to higher frequencies. It is possible to use an equalizing circuit which will correct for the rising characteristic of this type.

Table VI. Improvement Due to Double Acoustical Floor in Motor Coach

Frequency Bands	Conventional Floor VVL	Acoustical Double Floor VVL	Reduction in Vibration Decibels
0–64	45	45	0
64–256	70	68	2
256–512	72	67	5
512–1,024	72	62	10
1,024–2,048	69	56	13
2,048–4,096	64	52	12
4,096–8,192	42	34	8

(2) Amplifiers

The design of amplifiers covers a wide range beyond the scope of this paper. In general, high gain of the order of 120 decibels with a flat frequency characteris-

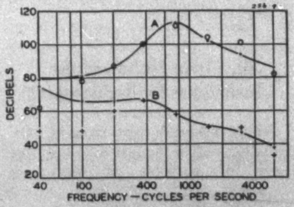


Figure 9. Train noise

Graph of a frequency analysis at 60 miles per hour. Pressure level versus frequency. Readings were taken with filter band widths of one octave centered at points shown
Curve *A*—Noise underneath
Curve *B*—Noise inside
Circles—Loudness level underneath
Crosses—Loudness level inside

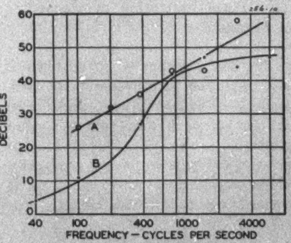


Figure 10. Train noise

Graph of difference in pressure level underneath and inside versus frequency
Curve *A*—Train stationary with noise source a loudspeaker on track
Curve *B*—Train running, producing its own noise
Data averaged from six speeds over the entire speed range

tic is needed. Commercial sound level meters are now provided with two frequency weighting networks. Amplifier circuits usually include an attenuator graduated in decibels. The complete reading consists of the attenuator setting plus the final meter reading.

(3) Filters

For obtaining a single figure representing pressure level, commercial meters, as mentioned in paragraph 2, have provision for a flat frequency characteristic in the 100 phons loudness level range, and two weighting networks, related to the equal loudness contours of Figure 1, one for the 70 phons and the other for the 40 phons range. The author prefers to make frequency analyses with a band pass filter, covering the audible range of octave steps. The filtering action is obtained with coil and condenser circuits controlled by a single tap-switch. Narrow pass filters are available with band widths as small as five cycles. There is another type with a band width of a fixed percentage of the frequency to which it is tuned. Both the heterodyne method with a fixed filter and the selective amplifier method operating on the degeneration principle are available. The types which permit aural monitoring with a telephone receiver are useful, as important components in the original noise may be identified by ear in a preliminary survey. Narrow band filters can be used to obtain accurately the frequency of a disturbing component. In certain cases this can be related to known occurrences such as tooth contact frequency in a particular pair of gears. Sources are sometimes identified this way.

(4) Final Indicating or Recording Means

The useful output of an amplifier is a-c. This can be delivered to an oscillograph or oscilloscope, or it can be rectified and indicated as rms volts on a meter graduated in decibels. The oscilloscope gives visual evidence of the complexity of the original sound wave, but its use is normally confined to the laboratory. For field work, good results are obtained by the rms type of meter in conjunction with frequency analysis. Level recorders are available for making charts of pressure level variation with time. For the particular problem of measuring reverberation time in room acoustics, a recorder which can follow quick changes in level is needed. Decay characteristics as high as 200 decibels per second are encountered in broadcasting studios, for example. The level recorder is useful in making frequency analyses in narrow band widths. The analyzer and recorder are driven by synchronous motors so that the abscissa can be read in terms of frequency, and the ordinate in pressure level of the passed band.

Figures 2–5 show some of the individual units described above. Figure 6 shows a sound-level meter ordinarily used with a crystal microphone, here used with a crystal type of vibration pickup. This particular design has a control box with integrating circuits which may be set for displacement, velocity, or acceleration. The photograph shows it set for velocity. It will then give a single reading proportional to the velocity of the stylus which would turn out, for the average field problem, to be due to the single most intense frequency component of velocity. For information regarding less intense components, a frequency analyzer can be used between the pickup and the sound-level meter. Without analyzer equipment, a rough check on frequency distribution can be obtained by displacement and acceleration readings, and by using the weighting networks on the sound-level meter. Figure 7 shows a set-up for making an automatic frequency analysis with a graphic recorder. With elaborate equipment of this type, data can be taken rapidly and in the detailed form necessary for the complete handling of acoustical problems.

III. Train Noise and the Techniques for Reducing It

Certain assumed measurements have been discussed in the first section of this paper for purposes of illustrating the principles involved. Measurements on actual train noise made by the author are presented in Figures 8–10. These data have been selected as representing the over-all aspects of the problem. No attempt is made in this paper to compare various designs. The data selected, however, are from extensive tests on a rather quiet type.

Noise and Vibration Paths

In Figure 11 are indicated schematically the various paths that sound energy from three representative sources may take on its way into the coach to disturb the passenger. The noise radiated from the wheels and trucks and the shocks delivered to the center plate depend upon spring design. A discussion of the reduction of these disturbances by design modifications is outside the scope of this paper. On the benefits of rubber springing see, for example, the paper by Hirshfeld and Piron.[7]

The simplest way to control a noisy source is to surround it with a barrier having an adequately high transmission loss (*TL*). The air-borne noise radiated from the compressor surfaces of Figure 11, for example, could be reduced in this way. There may be practical considerations such as space limitations or need for free air flow for cooling the machine. The direct structural path between the compressor and the floor may transmit vibrations which will reradiate from the floor to the air inside the coach. If necessary, the compressor may be isolated with resilient mountings. Proper isolation provides flexible connections to the machine. With due precautions for maintaining alignment between driving and driven units, mountings of high compliance and, consequently, high isolation may be used.

Direct leakage of air-borne sound through openings may nullify the results of careful work on other phases. Such

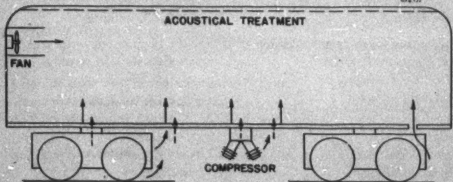


Figure 11. Schematic diagram of noise sources and energy paths in coach

Solid arrows—air-borne path

Dotted arrows—structural path

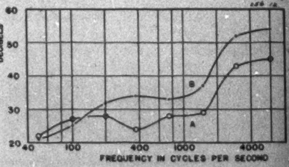


Figure 12. Transmission loss of coach floor

Graph of *TL* versus frequency

Curve *A*—Untreated floor

Curve *B*—Floor with deafening plate screwed to floor members and rock-wool blanket between floor and plate openings should be kept at a minimum. For example, a good window construction having a TL of 30 decibels, if opened two inches, is no better than a very ordinary construction with a TL of 20 decibels kept closed.

A fan of an air-conditioner system may deliver noise along with the air. Large reductions are possible by treating a run of duct with sound-absorbing material.

The floor is subjected to an intense air-borne noise as indicated in Figures 8 and 9. It also is vibrated by way of the structural path from the wheels. Figure 10 indicates that the inside pressure level at the middle frequencies is due to the transmission of air-borne noise. At the lower frequencies it appears that structural vibration contributes importantly to the inside pressure level. The TL of the floor can be increased by various means, and it can be isolated from the structure. If the floor must help in adding strength to the structure, isolation is not feasible. An extra floated floor may be a possibility.

Sound Barriers and Enclosures

Structures with substantially rigid connections throughout have a transmission loss linearly proportional to the logarithm of the weight per square foot. See for example the work by Knudsen[8] and many others. The average TL at the nine frequencies commonly used is expressed by

$$TL = 22.4 + 14.3 \log_{10} w \qquad (10)$$

The weight w for a typical coach floor is ten pounds per square foot. Substituted in equation 10, this gives an average TL of 36.7 decibels. For convenience, this equation may be thought of as giving the TL at 1,024 cycles. Measurements on a wide variety of homogeneous structures show that for a fixed weight the TL increases four decibels per octave, and for a fixed frequency increases four decibels on doubling the weight. These are averages for many tests. A single panel or wall may deviate five decibels or more from these predictions at any one frequency due to resonances. The average over the frequency range usually will be within two decibels of equation 10.

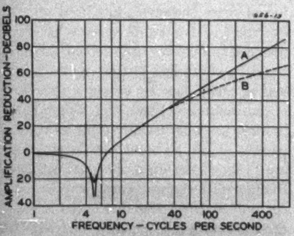


Figure 13. Theoretical vibration reduction due to isolation

Decibels change from rigid mounting versus frequency of the driving force. System has natural frequency of 4.95 cycles, showing amplification at all frequencies below seven cycles

Curve *A*—Zero damping

Curve *B*—Commercial amount of damping

Table VII. Improvement Due to Vibration Isolation

15-Hp Motor on Concrete Slab

	Slab Vibration Velocity Levels			Reduction—Decibels	
Frequency Bands	Motor Bolted Directly	Motor on Springs	Motor on Rubber	By Springs	By Rubber
0–64	105	72	85	33	20
64–128	83	90	62	33	21
128–256	81	51	49	30	32
256–512	76	53	47	23	29
512–1,024	65	45	44	20	21
1,024–2,048	54	29	29	25	25
2,048–4,096	43	10	23	33	20

If we wish to improve the TL of the above floor by ten decibels, a single floor weighing 50 pounds per square foot is indicated. This would be prohibitive in any practical transportation problem. Broadcasting-studio construction methods can be used in a suitable way. The TL of a three-pound-per-square-foot barrier by equation 10 is 29.2 decibels. Using our original figure of 36.7 decibels, we might hope that with a three-pound extra floor properly applied, the losses would add to a total of 65.9 decibels. Because the two must be used close together and joined compliantly to the same structure or to each other, the indicated high total is not reachable. However, by proper structural isolation, and using sound-absorbing material between the two floors, 48 decibels may be obtained. Thus, we have exceeded the estimated performance of a 50-pound homogeneous floor by using a special 13-pound composite floor.

Table VIII. Improvement in Decibels Due to Vibration Isolation

Train Floor on Shear Rubber Isolators Compared to Floor Mounted on Steel Blocks

Frequency	Design A $^3/_8$-Inch Thick—0.2-Inch Deflection	Design B $^3/_8$-Inch Thick—0.4-Inch Deflection	Design C $^3/_4$-Inch Thick—0.3-Inch Deflection
100	8	10	21
225	8	15	20
375	7	21	12
512	12	22	21
750	10	11	14
1,024	9	13	10
1,500	9	17	14
2,048	9	12	15

Actual field figures on the vibration performance of a double acoustical floor in a motor coach in terms of vibration velocity level are presented in Table VI. The isolated element was only one pound per square foot with rock wool between the floors. If the base floor had been increased homogeneously by this amount, the decrease in VVL would have been negligible. By adding this light weight with proper attention to the acoustical principles involved, a substantial decrease in VVL of the important radiating surface was obtained.

The addition of sound-absorbing material and a deafening plate, screwed to the under side of the floor-stiffening members, provides a smaller but definite improvement. Figure 12 shows transmission loss versus frequency for a typical coach floor tested in the laboratory, using a large test section 86 inches by 78 inches. The walking surface was $^5/_8$ inch Chanarch poured with sufficient magnesite to fill in the slots of the Chanarch and extend above it one-half inch. Five two-inch Z bars provided stiffening. Are movable 22-gauge deafening plate was screwed to the Z bars.

Vibration Isolation

A vibrating piece of equipment, such as the compressor mentioned above, radiates air-borne noise directly from its metal surfaces and usually vibrates the structure to which it is rigidly bolted. The structural vibrations will travel long distances with negligible loss, and cause trouble by radiating sound inside enclosures which have sufficient TL to bar the air-borne sound striking them directly.

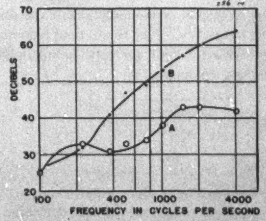


Figure 14. Transmission loss of coach floor

Graph of TL versus frequency

Curve *A*—Untreated floor

Curve *B*—Floor with deafening plate attached to floor members by shear rubber isolators and rock-wool blanket between floor and plate

The remedy consists in isolating the equipment by rubber or springs. The improvement may be striking, and, where this step may be taken, it is the most important single method available.

Table VII shows the large reductions brought about by a commercial type of isolator using the controlled spring principle in combination with rubber. This particular application was to lessen the vibration in the concrete slab under a 15-horsepower motor. Vibration velocity levels of the slab are given. Data on the conventional type of bonded rubber isolator used in shear are also given. It was less efficient than the controlled spring design, but the reduction in vibration by its use was large. For these tests, the set of isolators, both spring type and rubber type, were loaded to 0.35-inch deflection.

The same principle may be used in isolating the floor from the supporting structure. Table VIII gives actual reductions by laboratory tests on such a construction using rubber in shear. An electrical driving unit was attached to the structure so that it vibrated the subframe at the desired frequency over the entire frequency range. The vibration velocity level of the floor surface was measured at each frequency with a rigid structural connection and with three variations of shear rubber isolators.

Data such as these indicate by actual tests that best isolation results are obtained with the greatest deflection permissible, and, for a given deflection, it is advantageous to use the thickest rubber.

If a motor is to be isolated so that its vibrations are to be kept out of the structure, or a floor is to be mounted so that vibrations in the structure will not enter the floor, theory calls for suspending the motor or floor by compliant means so that the natural frequency of the suspended system is well below the disturbing frequency, commonly called the driving frequency. The theoretical reduction in decibel form,[9] for any value of the driving frequency, is

$$20 \log_{10} \frac{\sqrt{(c\omega)^2+(k-m\omega^2)^2}}{\sqrt{k^2+(c\omega)^2}} \tag{11}$$

where

m = mass of suspended weight (slugs)

k = pounds to produce one-foot deflection in the compliance

c = pounds force required to maintain unit velocity

$\omega = 2\pi f$ where f is the driving frequency in cycles per second

Table IX. Theoretical Reductions Due to Vibration Isolation for Various Static Deflections

Commercial Amount of Damping

Static Deflection Inches	Natural Frequency—Cycles Per Second	Reduction—Decibels Driving Frequency—Cycles Per Second 20	50	100	200	500
1/16	12.56	4	23	35	44	54
1/8	8.86	9	29	39	48	58
1/4	6.28	18	35	44	52	61
1/2	4.43	24	39	48	56	64
1	3.13	31	44	52	59	67

Experience has indicated that the actual decibel reductions obtained in commercial installations are less than half of the theoretical values.

Good isolation calls for mountings soft enough to deflect one-eighth inch or more under the static load of the suspended part. The natural frequency f_n in cycles per second is

$$f_n = 3.13\sqrt{\frac{1}{\delta}} \tag{12}$$

where δ = static deflection in inches

Isolator *B*, for example, of Table VIII supported 40 pounds per inch of isolator with a deflection of 0.4 inch. This means a *k* value of 1,200 pounds. Using an isolator one inch long the damping factor *c*, evaluated by the methods of reference 9, was 0.23 per inch of isolator. Most commercial materials have damping factors similar to this, which are small in comparison to the masses and stiffnesses involved.

A plot of equation 11 for $c = 0.23$ and $c = 0$ is given in Figure 13, using $m = 40/386$ and $k = 1{,}200$. This is a plot from theoretical considerations. The natural frequency is 4.95 cycles per second, and for a driving frequency of this value there is an amplification of 23 decibels with the commercial damping measured, and an infinite amplification for no damping. The curves cross the 0-decibel line at $\sqrt{2} \times 4.95 = 7.0$ cycles and show increasing isolation for higher frequencies. Above seven cycles the damped curve lies below the non-damping curve, but the theoretical detrimental effect of a small amount of damping is of less practical importance than the departure of actual systems from the simple one-degree-of-freedom system here assumed.

Table X. Improvement Due to Ceiling Acoustical Treatment in Coach

Frequency	Sabines Absorption Before	Added	After	Reduction—Decibels
128	140	150	290	3.1
256	225	336	561	4.0
512	354	594	948	4.3
1,024	354	594	948	4.3
2,048	314	546	860	4.4
4,096	265	492	757	4.6

The theoretical value of using the softest mounting possible, from the isolation standpoint, is seen in Table IX. As noted above, actual reductions are of the order of one-quarter to one-half theoretical. Above resonance a useful simplified form of the isolation equation, corrected so as to give the range of actual reduction is

$$10 \log_{10} \frac{f}{f_n} \text{ to } 20 \log_{10} \frac{f}{f_n} \tag{13}$$

where f_n = natural frequency of the suspended system

f = driving frequency

Instead of screwing a deafening plate directly to the floor members, as in Figure 12, isolation may be utilized. Figure 14 shows transmission loss versus frequency taken in the laboratory for a typical coach floor 86 inches by 41 inches. The deafening plate was attached by shear rubber isolators, and rock wool was used between the plate and the test floor. The improvement was very marked.

Panel Damping

The addition of damping compounds to vibrating panels is useful in shortening the decay time of transient vibrations, and in lessening the response at resonant frequencies. Automobile doors, for example, when slammed, emit a clanging tinny sound, due to exciting segmental vibrations in the thin gauge metal by the sudden shock. With a damping compound added, the slam noise becomes a dull thump, and the improvement is remarkable. The usefulness of this method for controlling the vibrations of forced drive, however, has been greatly overrated. If a panel system has pronounced resonances from forced drive, it could be controlled by this method, but it is so easily done by touching the point of maximum amplitude that this type of problem does not usually come to the attention of the acoustical engineer. Figure 15 shows the effect of a good damping treatment of the conventional type on a circular 18-gauge panel mounted in rigid clamping rings 12 inches internal diameter. The panel was driven through resonance by an electromagnet actuated by an audio oscillator and the pressure level three inches away measured. A drop of 20 decibels at the resonant frequency was attained, but away from resonance the levels were changed very little. The resonant peak for the treated panel is at a lower frequency because the treatment increased the effective mass of the panel without adding sufficient compensating stiffness.

Ceiling Acoustical Treatment

The average energy density built up by a source of fixed acoustical power in a closed space is inversely proportional to the sabines absorption in the space. If the space has a_1 sabines initially, and the total number of sabines is increased to a_2 by adding acoustical treatment, the decibel reduction in pressure level will be

$$10 \log_{10} \frac{a_2}{a_1} \qquad (14)$$

Equation 14 shows that relatively large reductions will be obtained by treating a given ceiling area, for example, with a material having an 0.85 coefficient if a_1 is small. If a_1 is initially large, then smaller reductions will be obtained.

The sound-absorption coefficient varies with frequency. The most efficient commercial material having the largest sale today has the following coefficients:

128	256	512	1,024	2,048	4,096	*NRC*
0.25	0.56	0.99	0.99	0.91	0.82	0.85

The last value is the noise reduction coefficient, which by current practice is the linear average of the coefficients at 256, 512, 1,024, and 2,048. It is assigned in an effort to give a single figure rating of a material. The author, however, believes in judging noise reduction by the improvement over the frequency range.

Suppose the inside of a coach is 60 feet by 10 feet by 9 feet high. Assume 40 upholstered seats at seven sabines each (at 512 cycles) and 2,460 square feet of metal, glass, and floor surface at an average coefficient of 0.03 (at 512 cycles). The total sabines at 512 cycles are $40 \times 7 + 2{,}460 \times 0.03 = 354$. Suppose that the 600 square feet of the ceiling is to be treated with the above material. The original steel ceiling surface contributed 18 sabines at 512, which would then be covered. This is not enough to affect the final result, and this small correction will be neglected here. Making some justifiable assumptions on the sabines originally present at frequencies other than 512 cycles, we can prepare Table X, showing the sabines before and after acoustical treatment and the decibel reductions computed by equation 14. The reductions are of the order of 3–5 decibels. If we had started with a coach having less sabines initially, such as the suburban motor type with leather seats, the reduction by adding an acoustical treatment would be higher, ranging up to eight decibels in the middle and high frequencies.

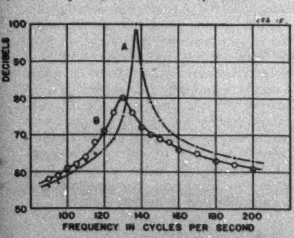


Figure 15. Vibration reduction due to damping

Graph of pressure level versus frequency.

Test panel excited over resonance range by electromagnet driven at various frequencies. Pressure level taken three inches away from panel center

Curve *A*—Bare 18-gauge panel

Curve *B*—Panel with $^5/_{32}$-inch commercial damping felt cemented to it

The contribution of ceiling acoustical treatment to human comfort, particularly in making conversation easier, seems to be out of all proportion to the decibel reductions figured on a sustained tone analysis.[9] Reverberation times are cut in half by treatments which account for a three-decibel reduction in average pressure level. It may be that it is this shortening effect on rail clicks and rattling dishes and silverware which accounts for the substantial improvement in ceiling-treated dining cars, for example. These die away quickly, and the syllables of conversation, maintained for a relatively long time, are heard. High frequencies which might reflect back on the passenger from a hard ceiling are partially absorbed with an acoustical ceiling.

Duct Acoustical Treatment

Fans and air conditioners sometimes cause trouble due to air rush and fan or equipment noise delivered with the air. If a run of duct is available in which acoustical treatment may be placed, the noise delivered to the passengers can be reduced greatly. Papers by Parkinson[10] and Sabine[11] deal with the practical aspects.

A rule of thumb for lining ducts is to treat a length equal to at least ten times the smallest duct dimension. The reduction obtained varies of course with the efficiency of the absorbing material and to some extent with the size of the duct. For a six-inch square duct lined for sixty inches with a material having a coefficient of 0.50, Parkinson's curves show 14 decibels, and Sabine's formula computes to 16 decibels. Fan and air-rush noise covers a wide frequency range. It has been found fairly satisfactory to correct for 256 cycles. The reductions at other frequencies which are obtained by the usual acoustical duct-lining materials are then acceptable for most applications.

Figure 16 gives results on silencing a difficult conditioner problem. The only available place for treatment was at the air-discharge grille in a space 48 inches by 76 inches cross section, and it was required that the treatment be 30 inches long. Lining only the sides of such a short run of this large section would be inadequate. The treatment adopted was one-inch rock-wool boards placed five inches on centers dividing the space into 13 flues each 30 inches long and 48 inches by 4 inches cross section. Data taken with an octave band pass analyzer have been plotted at frequencies near the midpoint of the bands, and a smooth curve drawn. The grille was in the ceiling, and the readings were taken at head level beneath it. Due to the predominance of the band centered at 375 cycles, the total noise was reduced by the order of four decibels, but reductions in the various bands ranged up to 20 decibels. By ear the improvement was very marked. Before treatment, the noise generated by air rush and water sprays could be heard the full length of the corridor beneath the grille—about 50 feet. After treatment, it was necessary to stand directly under the grille to hear the disturbance.

Conclusion

Acoustics is a reasonably complex science. Good instrumentation is available for attacking practical problems. The quiet train ride is obtainable by a rational application of known techniques,

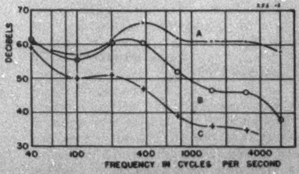


Figure 16. Noise reduction by treating air-conditioner duct

Graph of pressure level versus frequency

Readings were taken with filter band widths of one octave centered at points shown

Curve *A*—Original noise beneath grille

Curve *B*—Noise after treatment was installed

Curve *C*—Background noise with conditioner not running developed in the laboratory by careful measurement and used in the problem as indicated by the proper interpretation of the instrument readings.

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A Turbine-Governor Performance Analyzer

Discussion and author's closure of paper 41-158 by W. O. Osbon, presented at the AIEE South West District meeting, St. Louis, Mo., October 8–10, 1941, and at the AIEE winter convention, New York, N. Y., January 26–30, 1942, published in AIEE TRANSACTIONS, 1941, November section, pages 963–7. The following discussion was presented at the 1942 winter convention.

P. B. Juhnke (Commonwealth Edison Company, Chicago, Ill.): Concerning Mr. Osbon's paper many questions come to one's mind. We recently completed a series of measurements to determine the sensitiveness of the governors of the machines on our system, and, curious to say, we found a range considerably beyond anything we had anticipated, some of the larger and most modern units having a regulation ranging up to 20 per cent; whereas machines of older vintages had a regulation of 10 per cent or less. Perhaps the underlying reason for the poorer regulation of the latest machines is that they were intended for base load, and this poorer regulation was thought to be an asset in that field. These premises for future machines may well be reviewed as to their validity, as the subject of system regulation during the last five years has made tremendous strides, and a lower per cent of regulation in the future should no longer be the liability it was once thought to have been.

R. Sheppard and **C. Concordia** (General Electric Company, Schenectady, N. Y.): There has been considerable discussion recently of turbine speed-governing characteristics and of their effects on power-system operation. Of the many aspects of governor response, it has previously been indicated[1,2,3] that probably two of the most important are the dead or insensitive frequency band, within which the governing system as a whole will not respond, and the steady-state incremental speed regulation (the ratio of speed change to load change for a small change from normal speed). That is for the reasons that on most large power systems, with their accompanying large rotary inertias and relatively small load changes, the frequency changes are rather slow, and the principal contributing factor in delaying the governor response is not the dynamic lag, due to governor inertia, oil flow, steam, or water storage, and so forth, but the frequency dead band, due to backlash, friction, valve lap, and so forth. It is evident that the slower is the frequency change, the more apparent the effect of dead band becomes, and the less the dynamic lags appear. Dead band appears inherently as a frequency lag (that is, a more or less definite small frequency change is required before the control mechanism will respond), while the dynamic effects appear inherently as time lags. Thus with small frequency changes and long times involved, the relative importance of dead band becomes obvious.

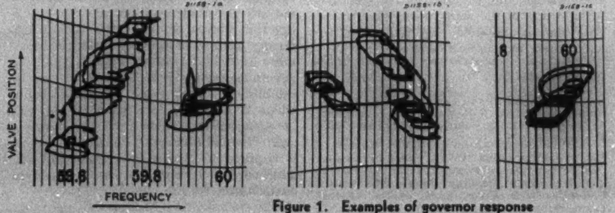

Figure 1. Examples of governor response

Moreover, with the small frequency changes usually encountered on large power systems, the effective magnitude[2] of the machine response, that is, the effective incremental speed droop, is greatly affected by dead band. This does not imply that one can forget the dynamic lags. These lags, together with the incremental regulation, on the one hand, affect the degree of frequency stability when the machine is not connected to the system during synchronizing, and, otherwise, when the machine is connected to the rest of the system, affect the system frequency fringe, particularly the magnitude of self-excited frequency oscillations produced by dead band.

Because of these considerations, it has been concluded that dead band should be made as small as possible.[1,2,3] Once such a conclusion has been reached, it becomes desirable to know what can be done, and how far one should go to produce improvement in those systems requiring it. A first step is to determine the characteristics of governing systems presently in operation. The results of a few of such measurements of governor response which have been made on turbines operating on several power systems are reported in this discussion.

Description of Measuring Instrument

The measuring device which has been designed and used to determine the significant response characteristics of turbine speed-governing systems is similar in principle to that developed and recently described[4] by Mr. J. E. Allen of The Pennsylvania Water and Power Company. Some of its features are:

1. The instrument shows directly the principal characteristics of interest: dead band and incremental speed regulation. No replotting or working up of data or complicated interpretation of the results is necessary, except for multiplication of the valve travel record by a factor to take account of the lever ratio between valve motion and carriage motion, and of the ratio between turbine kilowatt input change and valve motion at the point in question.

2. The measurement does not interfere with normal operation of the machine and requires little time. Shutting down or even any special loading schedule is not required, and no restraint is imposed on the normal or emergency functioning of the governor.

3. The indications are accurately recorded.

4. The indications are easily understood and easily interpreted in terms of both the effect of governor performance on system operation and the effects of governor characteristics on governor performance.

Figure 2. General Electric photoelectric frequency meter with rocking chart carriage for turbine-governor tests

The instrument used in most of these measurements is essentially an adaptation of the engine-indicator card principle, in which system frequency is automatically plotted against steam valve motion. Thus, referring to Figure 1 of this discussion, as the frequency starts to change from, for example, an upward swing to a downward swing, the turbine valve may not respond until a definite change in frequency has been attained; then the valve will begin to move at a rate depending on the ratio of the rate of change of frequency to the incremental regulation. The two most important factors, dead band and incremental regulation, are thus shown very simply on a single record. Moreover, as there are always minor frequency swings occurring on the system, no special means for producing the motions are required. Experience has shown that the changes are usually sufficiently slow so that the dynamic lags do not unduly affect the pictures obtained. To obtain data on the more rapid transient response which may occur with large load changes or loss of load, oscillographic methods have been used.

In the tests reported here, frequency is recorded by means of a General Electric photoelectric frequency recorder on a card mounted on a carriage mechanically connected by a wire or cord to the steam valve, operating piston, governor beam, or to any point the motion of which is desired. The motion is usually amplified by a lever or crank in order to obtain a suitable plotting

Figure 3. Turbine-governor response recorder in use

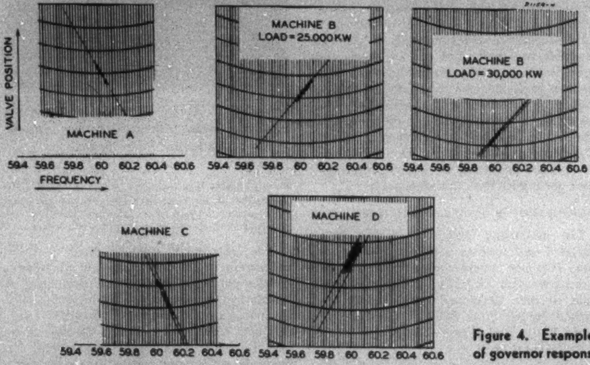

Figure 4. Examples of governor response

scale. It is thus possible to determine the amount of dead band contributed by each component of the mechanism. The frequency recorder, with the carriage for recording mechanical motions in place, is shown in Figure 2 and in use in Figure 3 of this discussion.

FURTHER EXAMPLES OF TEST RESULTS

Figure 4 of this discussion shows examples of the records obtained on a few of the machines tested. Machines *A* and *C* have two-stage hydraulic relays, while machines *B*, *D*, and *E* have only one-stage relays. The dead band is seen to vary from practically zero to about 0.1 per cent in these machines. However, because of the relative scarcity of data, it cannot be inferred that these values are representative of all machines, or even that they indicate probable values of dead band on other machines. Parallel lines have been drawn on some of these charts in order to illustrate further the procedure used to determine dead band, which is taken as the horizontal distance between the parallel lines and incremental regulation, which is proportional to the change in speed, or frequency, divided by the change in valve position.

CONCLUSION

In conclusion, it may be remarked that this method of governor testing to determine dead band and incremental regulation has shown itself to be very useful and informative. Further similar tests can probably be made by interested power companies, with mutual advantage to these companies and to the manufacturers of turbine-governing equipment. We take this opportunity to acknowledge the co-operation of our associates and of the many power companies that have contributed time and effort to make these measurements possible.

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W. O. Osbon (In collaboration with A. F. Schwendner, nonmenber; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Concerning Mr. Juhnke's remarks we are assuming that he means over-all steady-state regulation with the speed changer in the no-load position when he refers to a regulation of 20 per cent. Present practice for larger central-station units calls for four to six and not more than eight per cent over-all steady-state regulation. Six per cent regulation should satisfy most central-station generating systems. Load limiting devices which can be adjusted to relieve the governor of control are supplied with units intended for use as base load units. In any event the instruments described in the paper will aid greatly in measuring the speed regulation of existing machines and in determining the requirements for new ones.

The discussion by Messrs. Sheppard and Concordia brings out the fundamental differences between the governor analyzer and the instrument used by them. The governor analyzer was developed primarily for the purpose of giving information about the performance of each of the elements which make up the complete governing system. It shows not only the over-all response of the governor, but reveals also how much each individual element contributes to the final result. The instrument described by the discussers, on the other hand, gives only the values of frequency dead band and incremental governor regulation. Other instruments must be used to determine the elements of the governing system responsible for the result. It serves well to give a rough quick determination of dead band, provided that the rate of frequency change is smaller than the rates of response of the governor and of the recording instrument. If the rate of frequency change is higher, the width of the recorded dead band will be distorted by the lack of response. The photoelectric recorder used by the discussers is definitely limited in its rate of response. This effect is apparent in Figure 1 of the discussion where certain of the loops are definitely oval in shape. When such records are obtained, there is no way of knowing that the lack of response is not due to dynamic lags in the governor itself, rather than to the limitations of the instrument.

The governor analyzer, on the other hand, is capable of following the most rapid frequency changes likely ever to be encountered. Consequently not only does the running chart obtained with the analyzer indicate the dead band, but also, by checking the motion of the valve against frequency changes of different rates, the maximum rate of frequency change that the governing system is able to follow can be established. The value of dead band, which is a steady-state phenomenon, can be separated definitely from the dynamic lags. It is admitted that considerable work is required to obtain these data, but the additional information obtained adequately compensates for the trouble.

The statement by Messrs. Sheppard and

Concordia that incremental regulation can be obtained directly by the use of their instrument is somewhat misleading. They correctly define incremental regulation of a generating unit as "the ratio of speed change to load change for a small change from normal speed." Their instrument, however, gives only the ratio of speed change to valve movement. Because of curvature in the valve lift characteristic, the ratio of load change to valve motion must also be carefully measured. The determination of the correct value of this ratio for a particular load point is as important as the measurement of the governor regulation itself. By recording simultaneously all the quantities required for a determination of incremental regulation of a generating unit, the governor analyzer leaves nothing open to question.

Messrs. Sheppard and Concordia state that no special loading schedule is required when using their instrument. This statement also is misleading. It is true that dead band and regulation can be measured at one particular load, but a complete investigation of governor characteristics requires measurements at a number of different load points. That the discussers recognize this fact, in spite of their statement to the contrary, is indicated by their Figure 4 in which measurements on machine *B* at two different load points reveal different values of incremental governor regulation.

Control of Tie-Line Power Swings

Discussion and authors' closure of paper 42-72 by C. Concordia, H. S. Shott, and C. N. Weygandt, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 306–14.

Earle Wild (Commonwealth Edison Company, Chicago, Ill.): The paper by Messrs. Concordia, Shott, and Weygandt is a valuable contribution to the study of supplementary control. The conclusions, however, are applicable in but few cases today. The optimum regulation is seldom needed, and most tie-line controllers are probably not used at over ten per cent of this rate. It is possible that there are some cases where limited tie-line facilities and large highly variable loads could use the principles developed. The Chicago experience does not indicate any trend in this direction, but does indicate that a reasonable, orderly and fairly slow correction is satisfactory, permitting the large momentary power swings to be absorbed by the inertia of the large interconnection, but—all important—having a continuous correction of the tie-line load trends of longer duration.

J. E. McCormack (Consolidated Edison Company of New York, Inc., New York, N. Y.): The operating companies are interested in the characteristics of governors, frequency controllers, and other regulating devices to the extent that these equipments function with each other and with the other electric and steam equipments in the generating station. The rate of response of a governor or a frequency controller is only one of a series that includes the rate of response of the boiler control equipment, stokers, coal pulverizers, and the like. In general, the maximum available rate of response of the present electrical equipment in the frequency or load-control schemes is faster than the rate of response of the steam plant. If faster response of the supplementary control system is required, attention must be turned to the steam plant in order to obtain the faster rate.

Two types of controllers are discussed:

1. The proportional type which operates on accumulated time error.

2. The floating type which operates on instantaneous frequency.

Once again, the operating companies are not interested in their fundamental differences but are concerned in the results of these controllers operating on the system. Either type will do a good job as long as the system frequency, time error, or tie line, and station loads are not permitted to drift beyond the allowable limits of the operating company. If these limits are exceeded, the controller will have distinct disadvantages. The operation of a controller outside of the limits set up by the operating company is academic, because during this period the operator removes the automatic control from service and uses manual control to restore conditions to normal.

The installation of the correct type of automatic control, on an interconnected system consisting of generating stations and tie lines of varying capacities, will result in a narrower frequency variation, a smaller time error, a better utilization of the tie-feeder capacity, a reduction in the load swings on a station, and a lower maintenance cost in the boiler plant.

C. Concordia, H. S. Shott, and **C. N. Weygandt:** The object of our paper was to determine the possibilities and limitations of tie-line controllers when an attempt is made to get the most out of them, on the assumption that the tie-line controller is the limiting factor, that is, that boiler equipments and so on, are more than adequate. However, we are in entire agreement with Mr. Wild that the rates of response thus found to be optimum are in general much higher than would be necessary or desirable on the more usual large interconnected system, and with Mr. McCormack that, if such high rates of response are actually used, it may be found that the response of the boiler and its equipment will be the limiting factor. This paper represents a second step in the complete analysis of a controlled power system, the first step being the analysis of speed governors[1] presented a short time ago, and the third step possibly being a more detailed consideration of boiler performance. The boiler and turbine performance have already been discussed in the companion paper.[2]

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2. SUPPLEMENTARY CONTROL OF PRIME-MOVER SPEED GOVERNORS, S. B. Crary and J. B. McClure. AIEE TRANSACTIONS, volume 61, 1942, April section, pages 209–14.

Supplementary Control of Prime-Mover Speed Governors

Discussion and authors' closure of paper 42-73 by S. B. Crary and J. B. McClure, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 209–14.

P. B. Juhnke (Commonwealth Edison Company, Chicago, Ill.): The paper presented at this session by Messrs. Crary and McClure is evidence of decided progress in a field in which, until comparatively recently, wrong notions and concepts were rampant, because thorough scientific analysis had not touched it before. I and many other operating men concerned with this subject were victims of conclusions only partly founded in the facts observed. We made observations and misinterpreted them. Observations were frequently made in connection with the loss of generating capacity, with effects expressed only in frequency variations in a downward direction, and contained in the formula: one per cent loss in generating capacity carries with it a drop in frequency of $^1/_{10}$ cycle, and vice versa. In reality, there was another effect, completely lost sight of for some time, presumably due to the all-absorbing interest which had attached itself to frequency and its maintenance as a principal factor in the operation of interconnected systems. Repeated inconsistencies in our observations and the results reported by New York in the preceding winter convention prevailed on us to make further investigations. And then we began a series of tests to definitely determine the merits of our previous conclusions. We isolated certain sections of our system to simplify the tests, the sections ranging between 60,000 and 100,000 kw. The load on them was totalized on one meter, the voltage was held constant, and then the effects of the variability of the load with frequency, through a range of four cycles, was observed. In this manner we obtained evidence that our previous conclusions were 500 per cent high, or only about 20 per cent right. The conclusions drawn from these tests may be laid down in the formula: $^1/_{10}$ per cent variation in frequency is attended by not more than 0.2 per cent variation in system load. How could we harmonize with this the observation, made over and over, that $^1/_{10}$-cycle variation in frequency was accompanied by one per cent variation in load? We searched and found that every such loss in generating capacity was attended by voltage reduction —not large, but enough so that 80 per cent of the change in generation, which took place whenever the entire interconnected system lost one of its large units, had to be allocated to this cause.

To more definitely corroborate this, we very recently conducted voltage tests to determine the precise dimension of the effect of voltage variation on the system load. During these tests all automatic circuit regulators were taken out of service and the system voltage varied approximately five per cent. We then found that on a system load of 1,720,000 kw this variation in voltage produced a load change of approximately 70,000 kw, or four per cent, giving a ratio of 0.8 per cent load change for one per cent of voltage change.

Messrs. Crary and McClure's paper may evoke some difference of opinion as to methods and detail, whether the governor should be depended upon in conjunction with controllers equipped with time bias only for speed regulation, or with frequency and time bias. It is one of those subjects on which a difference of opinion may be warranted. We of the Chicago group lean to the belief that a controller based on tie line with frequency and time bias is the *ne plus ultra,* as a time bias alone is somewhat slow of response, whereas frequency bias is practically instantaneous in restorative efforts.

Henry Kreisinger (Combustion Engineering Company, New York, N. Y.): In the design of steam generating equipment for rapidly changing load the following factors should be given consideration:

The response of combustion control in supplying fuel and air to the furnace in proportion to the steam demand. This includes the response of the pressure fan supplying air to the furnace, and the induced draft fan moving the products of combustion through the steam generating unit.

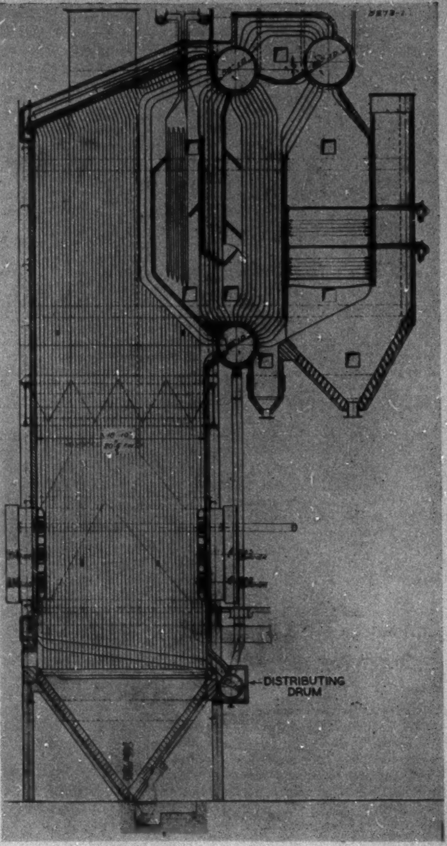
The availability of fuel supply for immediate use with large and rapid increase in load. With gas and fuel oil response is obtained in 5 to 15 seconds, with pulverized coal 15 seconds to two minutes depending on the magnitude of the load increase.

The repeating mechanical stresses imposed on the fan structure with large and rapid changes in load.

The repeating temperature stresses to which the metal of the heat absorbing surfaces is subjected.

The combustion control might be made so responsive that the fuel and air supplied to the furnace could be doubled in five seconds. The fans would have to handle twice the weight of air and gases against much higher pressure, and severe mechanical stresses would be imposed on the fans and the motors. The speed of the rotating parts could not be stabilized in such a short period of time; the speed would slow down and then speed up, and these changes in speed would be reflected in the draft in the furnace and puffy fire. The burning of twice the amount of fuel would result in twice the rate of heat transfer by the heating surfaces, which would require twice the temperature difference between the water in the boiler and the furnace side layers of metal forming the heat-absorbing surfaces. For high steam pressures the metal of the heating surfaces must be made thick, and this increased thickness results in proportionately higher temperature of the furnace side layers of the metal. Doubling the temperature difference causes large changes in the temperature of the furnace side layers of the metal which imposes severe temperature stresses in the metal. Such stresses when periodically repeated result in leaky joints, cracking, and corrosion of the tubes. Thinner tube walls would reduce these temperature stresses, but thinner tube walls would require small diameter tubes which in turn would require forced circulation.

Figure 1



With pulverized coal firing small load increases of about 15 per cent can be taken within about 10 seconds by drawing on the pulverized coal already in suspension within the mill by increasing the air flow through the mill. Most mills are provided with coal classifier which classifies the pulverized coal, allowing the finer coal to pass through to the burners and returning the coarser coal to the mill. Normally the amount of coal circulating through the classifier is five to six times the amount that passes through to the burners. When air flow through the mill is increased, the proportion of coal passing through is increased, and less is returned to the mill. The fineness of the coal going to the burners is only slightly decreased. Increased air flow through the mill also tends to put more coal in suspension, thereby reducing the amount of fines in the coal between the milling surfaces. Inasmuch as the fines have cushioning effect on the process of pulverization, the reduction of the fines in the coal between the milling surfaces tends to increase the rate of pulverization.

At the same time, when the air flow through the mill is increased, the coal feed to the mill is also increased. The rapidity of the response of the coal flow out of the mill to the increase of the coal feed is delayed, because the coal fed to the mill must be dried and pulverized.

The drying of the coal fed to the mill is facilitated by the greater flow of heated air through the mill. The delay in the response of the coal flow out of the mill to the increased feed of coal to the mill varies with the percentage of moisture in the coal. When very wet coal is fed to the mill, the response is apt to be slow, because the wet coal blankets the coal already in the mill, so that the fines are not readily picked up by the air flow. In plants where the coal is apt to be very wet, provision should be made for supplying hotter air to the mill to speed up the drying process. It is the coal already in suspension in the mill air that is immediately available for picking up and holding the load until the increased coal feed raises the rate of pulverization.

With a small drop in load the procedure is reversed, the air flow through the mill and the coal feed is reduced, and this reduction in turn decreases the amount of coal delivered to the burners.

Small changes in load put moderate duty on steam generating and steam temperature regulation. However, large changes in load impose a severe duty on the equipment and present difficult problems in steam pressure and temperature control.

With large and quick variation of load, it is difficult to follow the demand for steam with fuel and air supply to the furnace, and to vary the draft to move the varying weight of products of combustion through the steam generating unit. Even if such variation in fuel and air supply to the furnace could be obtained, there would be too quick and large variation in furnace temperature, which would cause severe temperature stresses in the metal of the heat-absorbing surfaces. Such stresses would be particularly severe with high pressures, because of the thick metal walls of tubes required for high pressures.

With the conventional natural circulation boiler fired with pulverized coal, the load could be raised from half to full load in about three minutes by doubling the fuel and air supply to the furnace, but this is not usually quick enough for most steel plants. In such plants the large and quick variation in the demand for steam is satisfied partly by varying the fuel and air supply to the furnace, and partly by drawing on the heat storage in the water and the steam space of the boiler. This stored heat is made available for immediate use by a small steam-pressure drop. The steam storage space includes the drums, the superheater, and the pipe line. When more steam is taken out of this steam space than is made by the combustion of the fuel, the pressure drops, and this drop in pressure causes part of the boiler water to flash into steam. The steam formation occurs throughout the body of the boiler water, and inasmuch as the steam has a much greater specific volume than the water, the water level in the steam drum rises. The rise in water level reduces feed water supply, thereby causing the flashing of the water into steam to be accomplished with smaller pressure drop. By the time the water level in the drum is lowered by the evaporation to a point when the feed water regulator would increase the feed to the boiler, the increased combustion rate is catching up with the steam demand or the peak load interval may be over. When the load drops, the pressure starts to rise, and the rise of pressure reduces the ebullition, thus causing further drop of the water level and increase of feed water supply. This increased water supply requires more heat to bring the water to steaming temperature, and this heat requirement gives the combustion control time to reduce the fuel supply to the furnace.

The steam-pressure drop during a large and rapid increase in load depends on the water content in the boiler and the steam space in the steam drums, superheater, and the pipe line. The larger the water content and the steam space, the smaller the steam pressure drop. A bent tube boiler with multiple steam drums of large diameter is best suited for such large and rapid changes in load, because it has large water content and steam space. Each tube is directly connected to one of the steam drums, so that the tubes can be quickly relieved of the excess steam during the peak.

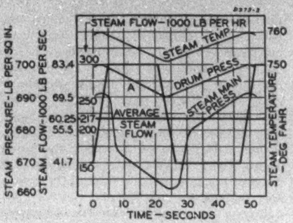


Figure 2. Steam pressure and temperature variation with rapidly changing load

A—Excess steam 432 pounds, six pounds from superheater, six pounds from steam drums, and 420 pounds from heat stored in water and metal
Water volume = 2,200 cubic feet = weight of water = 107,300 pounds
Steam volume in drums = 512 cubic feet
Steam volume in superheater = 230 cubic feet
Weight of metal in boiler = 442,000 pounds
Weight of superheater = 64,000 pounds
Water equivalent of boiler metal = 51,350 pounds
Total water equivalent = 107,300 + 51,350 = 158,650 pounds

In addition to the pressure drop in the steam drum, due to using stored heat in the boiler water for making steam, there is a pressure drop through the superheater and the pipe line. This pressure drop is necessary to make the steam flow and increases approximately as the square of the steam flow. That is, when the steam flow is doubled, the pressure drop is nearly quadrupled. The increase of this pressure drop may be two or three times the pressure drop, due to taking stored heat out of the boiler water.

The residual heat in the furnace is stored heat which works against good pressure and steam temperature control. The heat is stored during the peak load when it should be used for producing steam and is released during the low load period when no extra steam is needed. Therefore, the residual heat should be reduced to minimum by proper furnace design. Refractories and slag accumulations in the furnace should be avoided. The furnace should be completely water-cooled with dry bottom.

The steam generated at the peak loads with the heat stored in the boiler must be superheated with the heat stored in the metal of the superheater. Increased flow of steam through the superheater tubes increases the rate of heat transfer from the superheater metal to the steam, with the result that the temperature of the metal drops. The heat released by this drop of metal temperature is available for superheating the steam made with the heat stored in the boiler. When the load is dropped, heat is again stored in the boiler and the superheater. Superheaters of large weight of metal give more nearly constant temperature of steam with variable load.

When the high and low load periods are of comparatively long duration, the storage of heat in the boiler and superheater gives the combustion control time to catch up with the steam demands. When the periods are of short duration, the stored heat may take care of the load variation, and the fuel may be burned at constant rate. The latter case can be illustrated by the following specific example:

> A steam-generating unit is to supply steam for rapidly varying load. The steam demand rises from 150,000 pounds in five seconds to 300,000 pounds per hour; it stays at this peak for 16 seconds and then drops in five seconds to 150,000 pounds where it stays for 21 seconds. This load cycle is repeated every 47 seconds. The unit is to be fired with blast-furnace gas, with pulverized coal as supplementary fuel.

It is obvious that combustion control could not follow such large and quick variation. Even if it were possible to supply fuel and air to the furnace at such varying rate, it would not be good engineering to operate the unit in such a manner, because of the severe temperature and mechanical stresses imposed on the furnace and boiler heating surfaces and the fan equipment.

The unit selected for this load is shown in Figure 1 of this discussion. It consists of a three-drum bent-tube boiler with an additional small drum for distribution of water to the furnace walls. The two top drums are 54 and 60 inches in diameter; the water drum is 48 inches, and the distributing drum 27 inches in diameter. The furnace is completely water-cooled. The following are the principal data bearing on the pressure and temperature regulation:

Operating drum pressure	700 psi
Steam temperature	760 Fahrenheit
Boiler water volume	2,200 cubic feet
Weight of boiler water at operating pressure	107,300 pounds
Steam volume in drums and steam tubes	512 cubic feet
Weight of metal in boiler	442,000 pounds
Water equivalent of metal in boiler	51,350 pounds
Total water equivalent	158,650 pounds
Weight of superheater	65,000 pounds

The operation of the unit is illustrated in Figure 2 of this discussion, which is a chart showing load, steam temperature, and pressure variation with time. The abscissae is time in seconds. The ordinates are steam flow (the zigzag line), the steam temperature (the top curve), drum pressure (second curve from top), and steam main pressure (the lowest curve).

Fuel is supplied to the furnace at constant rate to generate 217,000 pounds of steam per hour, or 60.25 pounds per second, as shown by the horizontal line passing through this flow point on the scale. The area above this horizontal line and below the zigzag line of the peak load represents the quantity of steam made with the stored heat in the boiler, which is 432 pounds. It is also equal to the area below the constant-heat supply line and above steam-flow line during the low load period which represents the heat put in storage. The variation in steam temperature is nine degrees and is likely to be two or three degrees less, because of the increased rate of heat transfer from the metal of the superheater to the steam which has not been included in its determination.

The drum pressure drop is 10 pounds and is likely to be smaller, because the feed water supply will be reduced during the peak load and increased during the low load period. The effect of this variation of feed water supply has not been included in the determination of the pressure drop.

The variation of the pressure drop through the superheater and the steam main is the largest and is difficult to reduce. If the resistance through the superheater is made too low, the distribution of steam over the superheater may be uneven at low loads.

Earle Wild (Commonwealth Edison Company, Chicago, Ill.): The authors' conclusions in the first paper, excepting conclusion 2, confirm the opinions now held by the majority of operators in the largest interconnection in the country. Governors have been found by test to be the essential device for controlling the speed. In most of the turbines they were found to be active and sensitive, although the per cent regulation left considerable to be desired, especially in some of the newer and larger turbines. The governors very definitely determine the width of the frequency band. Changes in load demand with frequency and changes with voltage were found to be real, but of so much smaller magnitude that the effect of governor action is the only major factor in instantaneous speed control. As pointed out in the first conclusion, with good governors the rate of response of the frequency controlling stations can be much slower, since the error will integrate at a slower rate and correction can also be made at the source of the frequency error.

After recognizing the merits of the governor and the need for improvement in governors, its limitations must also be recognized. The three things that a governor will not do are:

Restore frequency to the tolerance band.
Correct time errors.
Allocate loads in the most economical manner.

These functions must be left to the supplementary controllers. This leads to another aspect of the problem, which has hardly been touched upon in any of the governor or related papers written in the last two years. In the large interconnection there is a load-control problem, which is entirely separate from the speed-control problem, and the importance of which has not been fully appreciated. In any local region of this large interconnection there will be dozens and perhaps hundreds of load changes in which there are no corresponding frequency changes, as contrasted with an occasional load change which is accompanied by a corresponding frequency change. Theoretically, if tie lines are unlimited in capacity, these hundred load changes could be ignored, and only the one which is accompanied by a frequency change corrected, but the fact remains that tie lines are limited in capacity, and no economical justification exists in the majority of cases for increasing their capacity or transferring load to other stations. The load-control problem must, therefore, be met, and this is best done through tie line controllers supplementing the governor action and using the tie-line bias principle, as correctly recognized by Messrs. Crary and McClure.

The periods of abnormal frequency in the large interconnection are caused by a number of local regions neglecting their load control problems at the same time. They are caused by such factors as system operators incorrectly prognosticating the system load, turbines or boilers being brought in too late for the load, the human element ignoring obvious indications of incorrect generation, inadequate telemeters with which to guide the operators. When a series of these maloperations cascade together, a gradual frequency change results, several times the width of the normal frequency departures, and considerably beyond the range of the frequency controlling stations. These occurrences are most severe during periods of rapid load change, when probably 90 per cent of the governors are being reset every few minutes to meet local load changes. Ordinary concepts of governor regulation during these periods are meaningless, because of this frequent resetting of the governors. In other words, if a governor calls for a three per cent load increase due to low frequency, while the operator is making a six per cent increase to meet local load demands, it is hardly expected that the operator would know he should make a nine per cent load increase in order to satisfy the governor. When the frequency was corrected finally, the operator would have to make an additional three per cent increase, in the interim not satisfying the low frequency requirements and increasing after the frequency is normal. This example of inadequacy of governors could be ideally handled with a supplementary tie-line controller. The major problem, then, is not what is the per cent regulation and the sensitivity of the governors, but why were so many turbines and boilers incorrectly loaded at the same time in widely scattered regions of the interconnection? The conclusion which has been reached, after each study of this problem, is that a wider application of supplementary controllers in local regions is needed, and it is very gratifying that the summary of Messrs. Crary and McClure bears out this opinion.

The major contribution of the Chicago area to this problem the past year has been the utilization of the tie-line controller impulses to vary the load of the three large variable load stations simultaneously. The burden of regulation on each one has become almost insignificant, while the deviations of the tie line from schedule are extremely rare, except in response to governor action during frequency changes. This confirms an earlier conclusion that a large part of the difficulty in regulating load and frequency comes from cross regulation between stations within any one group, and that the meeting of one per cent load change per minute is relatively easy. This experience is being extended to two other groups in this interconnection.

G. B. Warren (General Electric Company, Schenectady, N. Y.): The general question of what rate of change in temperature or what rate of change in load a turbine can stand is one of great complexity depending upon the conditions for which the turbine was designed, its capacity, its revolutions per minute, and the initial pressure and temperature at which the machine is operating. The change in initial temperature and change in load are related, since, even at constant initial temperature, changes in load bring about changes in internal temperature in the turbine, which may be exaggerated by the changes in initial temperature occurring simultaneously as a result of boiler conditions.

In a machine such as a turbine, through which steam is passing, the cross section of all parts cannot of necessity be made the same in relation to their surfaces in contact with the steam, and, hence, some parts are heated or cooled faster than others. This gives rise to changes in clearances and sets up strains and stresses, the magnitudes of which are a function of both the amount and the rapidity of the temperature change. This, in turn, may be a function of the rate and magnitude of the load change as well as the initial temperature change.

While exact data on the detrimental effect of these factors on turbines are not available, we have made careful efforts to ascertain from the operating people whether turbines subjected to varying loads require more maintenance than turbines subjected to more constant loads. There does not seem to be any great evidence to indicate that this is so, but in a few isolated cases turbines subjected to great changes in temperature and many washings without careful control of the temperature have appeared to require increased maintenance.

As a result of a careful survey recently made by a group of turbine designers in the General Electric Company, the following conclusions were reached:

> A plus or minus 75-degree-Fahrenheit change in internal temperature, either sudden or cyclical around a mean temperature, will not cause serious strains or clearance changes. These variations in internal temperature may be caused by variation in load or initial temperature or both. However, the maximum temperature reached must not exceed the contract limits both as to duration and intensity.
>
> With constant initial temperature, a 75-degree-Fahrenheit change in internal temperature generally corresponds to a change of load of approximately plus or minus 25 per cent of the load at which the turbine is operating.

When Messrs. Crary and McClure asked us for a statement of this situation, our preliminary investigation indicated that the 75-degree-Fahrenheit change corresponded to a change of about 15 per cent of rated flow. A more careful study indicates that a more accurate statement would be 25 per cent of the load or flow at which the turbine is operating. For two-thirds load this is not far from the statement in the paper.

The question of whether greater rates of change in temperature or load can be taken without increased maintenance is not so easily determined. The answer depends upon the size of the machine, the pressure for which it is designed and, hence, the thickness of the parts, whether the machine is an 1,800-rpm machine with a large diameter or a 3,600-rpm machine with a smaller diameter, and whether it is of single- or double-shell construction, and also whether or not it is a fixed back-pressure machine or a condensing machine. In general, the sensitivity of a machine to such changes in internal temperature decreases in the following order:

1. 1,800-rpm single-shell machines.
2. 3,600-rpm single-shell machines.
3. 1,800-rpm double-shell machines.
4. 3,600-rpm double-shell machines.

Condensing machines have a greater internal temperature variation for a given load change than noncondensing machines operating at a fixed and fairly high back pressure, and smaller turbines can probably stand a greater rate of temperature change than larger turbines, other conditions being comparable.

An effort is made to design all turbines so that in cases of necessity they can drop all load instantaneously without damage, or pick up full load from no load as fast as dry steam can be supplied.

Except when special circumstances alter the situation, we know that the most sensitive of the above machines have safely withstood the temperature changes incident to a sudden increase from zero to full load or of a sudden loss of full load. We do not know how many times in the life of a machine it can be safely put through such drastic changes in temperature without increased maintenance being required. In the case of temperature changes in excess of 75 degrees Fahrenheit, it would appear desirable to allow some time for the change to take place. Except under emergency conditions internal temperature changes should probably not exceed 75 degrees Fahrenheit suddenly plus five degrees Fahrenheit per minute for class 1 machines above, and for all other classes should not exceed 75 degrees Fahrenheit suddenly plus 10 degrees Fahrenheit per minute. Thus, an internal temperature change of 200 degrees Fahrenheit (whether from a load change or initial temperature change or a combination of both) should not take place in class 1 machines above in less than $(200-75)/5$ degrees $=25$ minutes. 200 degrees Fahrenheit internal temperature change corresponds to a change in load from about 50 per cent to full rated load, and twice this change will correspond to a swing throughout the full rated load. The conclusions which one would reach with respect to large changes of load from the above principles and simple calculations are not far from the 30 to 60 minutes suggested in the paper for a 100 per cent change in flow.

The above rules are intended to cover general cases. Some machines are particularly sensitive to temperature or load changes, in so far as temporary increases in vibration are concerned, and must, until such conditions are rectified, be accorded special consideration. Specific machines for especially high initial pressures or other peculiar operating conditions may also be exceptions.

These rules do not apply to changes in temperature, incidental to washing solids from the internal parts of turbines where water may be present in the steam with resulting increased heat content and conductivity of the steam, and resultant need for slower temperature changes and for which special instructions have been issued.

F. G. Sandstrom (Consolidated Edison Company of New York, Inc., New York, N. Y.): The morning rate of load growth of the entire Consolidated Edison Company load averages approximately 0.4 per cent per minute. On some stations this rate averages 0.5 per cent per minute. However, for short periods of about 10 minutes this rate may be as high as 2.0 per cent per minute at a station.

When our supplementary controllers were first installed, the rate of load change of the controller was high enough to take care of the short-time load changes. The steam group soon objected to this high rate of pickup because of the increased maintenance costs, objections to smoke and fly, has possibility of blowing a hole in the fuel bed, inability of maintaining steam pressure and so forth. The supplementary controllers are now adjusted for a rate of load change of approximately 1$^1/_2$ per cent per minute which the steam group feel is allowable on stoker-fired boilers.

As a result of our experience, we feel that the present supplementary controllers are fast enough. If they are used with too fast a rate of load change, the frequency and load-swing problems are not solved, and an additional problem may be created for the steam men.

The over-all governor response on our system is from 11 to 17 per cent, or several times the optimum rate quoted in the paper by S. B. Crary and J. B. McClure. There is nothing wrong with their theory that the smaller the regulation rate, the smaller will be the frequency swings. However, we are not concerned with frequency swings but are concerned by the load swings accompanying the frequency swings. These load swings are usually caused by the governors on the generators making up a deficit or an excess of generation on the system. If the system suddenly becomes short 50 megawatts, the load swings on the tie feeders and generating stations will be about the same, whether all the generators have 12 per cent regulation or 6 per cent regulation.

We have found that the cause of the load swings is usually incorrect anticipation of the change in the actual consumed load, or manual adjustments on turbine governors that tend to force the frequency away from 60 cycles. On the Consolidated Edison system load swings have been reduced considerably by furnishing the station operators with a synchroscope connected between a standard frequency and the system frequency. The standard frequency at each station is the Bell Telephone standard. The station operators pick up load when the frequency is low and drop load when the frequency is high.

On our system the stations are connected by relatively low capacity ties. An automatic supplementary controller that works on instantaneous frequency or time error would have to be constantly supervised by the operators in order to prevent overloading the tie lines and maintain the economy load schedule. Consequently, on a system similar to ours we feel that the tie-line bias control appears to be most suitable, because it supervises the tie-line loads and allows all stations so controlled to contribute to any large load changes that might occur. Then the station where the load change occurred can slowly at a controlled rate pick up the total load change in the area.

J. E. McCormack (Consolidated Edison Company of New York, Inc., New York, N. Y.): The conclusion of Messrs. Crary and McClure that absolutely flat frequency, zero time error and flat tie-line load are unnecessarily severe limitations may lead to the belief that a close approach to these conditions is desirable. I would like to point out that to approximate these aims, the controlling station or stations would have to withstand rapid load changes of a very high percentage of the station capacity. It appears to me that it is more desirable to let the frequency, time, or tie load drift in order to spread the load change over the entire system, so that the change on any station would be a small percentage of its capacity.

Automatic controllers cannot be expected to do all of the regulating, as the operator must make many manual adjustments. Machines must be started and loaded quickly to their minimum load, the regulating range on the regulating machine must be maintained, and the load has to be distributed between machines for economy reasons. The phase angle between bus sections must be maintained within allowable limits in stations supplying network load. To make these manual changes intelligently, the operator should have a knowledge of the response of the system and be provided with suitable instruments.

We have found a common frequency reference in all major stations to be useful when making manual load changes. A standard frequency is wired to the major stations where a synchroscope is connected between the standard and the system frequency. The synchroscope, being a very sensitive indicator of system frequency, is used by the operator as a guide in making manual load changes only at a time when they tend to restore system frequency to 60 cycles. Load increases are made when the frequency is low, and load decreases are made when the frequency is high.

Experiments have been made using a frequency standard transmitted by radio for comparing the system frequency by means of a stroboscopic image at each station. This method will be an economical way of obtaining a common frequency standard at the generating stations on a widespread system.

The trend toward larger interconnections made it imperative to have a thorough understanding of the effect of changes in load, voltage, and frequency on our system, in order to obtain the most advantages from the interconnection. The first consideration should be to obtain data for each system, which would require making tests allowing one of these three variables to depart from normal, while the other two are maintained normal. The carefully controlled departures of the system conditions from normal to obtain this vital information will result in a better understanding of system operation. The information will aid in obtaining the maximum utilization of the generation and tie lines, as well as aid in restoring conditions to normal in times of an emergency.

When this information is available, the next and most important job is to place the information into the hands of the operating force in the individual plants. Any steps taken to give the operators a better understanding of the importance of performing their operations correctly will produce surprising results in the improvement of system operation.

As an example, I discussed with our system operators the possibilities of a stroboscope using a radio signal for a common frequency standard. Two weeks later one of these men produced a working model of the idea. More important than obtaining this working model, the system operating group have become very interested in obtaining a better understanding of the frequency control problem. The operating men want to do a good job but are often prevented, because the problem has not been presented to them properly.

S. B. Crary and **J. B. McClure:** The authors wish to express their appreciation for the discussion on this paper. This subject is an important one to both operators and manufacturers, and their combined viewpoints should broaden their understanding of it.

Mr. Juhnke has cited an interesting series of tests on sections of his system to determine the relative effects of voltage and frequency variations on system load. His results check those previously obtained by Mr. J. E. McCormack, namely, that the system load is much more sensitive to voltage variations than it is to frequency variations. It is recognized that these results are peculiar to their respective systems and cannot be generally applied to all systems. Mr. Juhnke's point regarding the use of supplementary time bias in addition to frequency bias is well taken. We did not mention time bias in our paper, since it is ordinarily applied at such a slow rate that it may be added without difficulty. However, it is a valuable addition to automatic frequency and tie-line bias control.

Mr. Kreisinger's discussion of the factors involved in the design and performance of steam-generating equipment will be of particular interest to many electrical engineers who are not so familiar with the mechanical engineers' problems associated with governor performance and supplementary control on a system. We also appreciate his description of a boiler which is so designed as to be able to handle rapidly fluctuating loads. However, from our discussions with the operating groups, the older steam-generating equipments, in many cases, have distinct limitations in this respect.

Mr. McCormack's emphasis on allowing some leeway in the control of frequency, time error, and tie-line load is very much to the point. We had hoped that our conclusions were clear in this respect, particularly for the benefit of groups just embarking on this area of control. Other groups have been through the experience of attempting to force the control of frequency, time, and tie-lines to the limit of perfection and have invariably concluded that the intelligent solution is necessarily a compromise among the several requirements which must be considered in the complete operation of an interconnected system. His reference to the possibility of using a radio-transmitted common master frequency over the entire system indicates that manually controlled stations soon will be able to make their adjustment so as to always help rather than aggravate the over-all picture.

Mr. Sandstrom has cited rates of transfer on their automatic stations which are interesting, and to some who have not had experience with automatic control, and to others whose systems are relatively rough, these rates will appear surprisingly low. Again, this cannot be taken as a general answer, and every system will have individual characteristics in this respect. We concur with his conclusion that relative generator and tie-line loadings will be practically the same for a system equipped with 12 per cent governors as for one with 6 per cent governors. As soon as the operating philosophy recognizes that frequency departure is not important, then it is not so important to have the composite governor regulation at the optimum value. We merely tried to point out that governor as well as supplementary controller characteristics are important factors in determining the regulated frequency characteristic. We emphasized the optimum governor regulation, hoping that that viewpoint would be helpful to some who are advocating operation of their systems with governors having appreciably narrower regulation than at present, in establishing a lower limit of regulation below which no net gain would be obtained.

Mr. Wild mentioned their recent practice of extending the tie-line controller impulses to several stations simultaneously. The benefits of distributed control are so apparent that we can expect the number of regulating units to increase steadily.

The comments of Messrs. McCormack, Sandstrom and Wild on the importance of proper co-ordination of all manual control and the benefits to be derived from educational effort will be of particular interest to other operating companies. Manual control will always be present to some degree, and thorough appreciation of the problem and the objectives is essential to those assisting on manual control. Proper instruments are also essential, and, as the needs are recognized, improved instruments will be made available.

Mr. Warren and his associates assisted us in arriving at the statements made in the paper regarding rates of load transfer on the newer higher-temperature high-pressure steam turbines. We are pleased to have Mr. Warren outline in more detail the factors which influence the performance of a turbine under fluctuating load conditions.

PCC Car Operating Results in Pittsburgh

Discussion of paper 42-52 by W. J. Clardy, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 214–17.

H. G. Moore (General Electric Company, Erie, Pa.): Mr. Clardy, in his paper, has shown in great detail the economic advantages of modernization of street railway service by the use of PCC cars in Pittsburgh. This confirms by data, and adds to the list of universally favorable reports on the successful operation of really modern street cars in other cities.

For example, the first PCC car installation was in Brooklyn. Four lines equipped increased their revenue by 15, 24, 26, and 33 per cent in a period when other lines had decreasing revenues of one to five per cent. Schedule speeds were also increased 13 and 14 per cent on two lines where PCC cars are used exclusively.

Other figures, at random, may also be cited. In Chicago, riding increased approximately 12 per cent over the balance of the system, and schedule speeds were increased eight to ten per cent. In Baltimore, revenue increased eight per cent, and maintenance was reduced. In Los Angeles it has been estimated that the increased patronage provides increased receipts of $3,000 per car annually on one line, and $4,000 per car annually on another line. Washington reported somewhat increased schedule speeds in spite of operating on lines with older cars, 22 to 37 per cent decrease in platform expenses; 15 to 30 per cent decrease in accidents and 30 to 49 per cent reduction in maintenance costs. Philadelphia increased revenue 16 per cent and schedule speed 15 per cent.

While all of these figures compare operation of modern PCC cars with older types of cars, it should also be kept in mind that PCC cars compare even more favorably with other types of vehicles sometimes proposed for handling heavy-density surface traffic. I would refer to a paper[1] presented at the winter convention four years ago by Mr. C. M. Davis outlining the particular fields of public transit served best by one or another type of vehicle.

The principal governing factor is the traffic density, or the number of passengers to be carried per unit time in the direction of maximum movement. For the heaviest type of service, the rapid-transit system operating over private right of way under ground or overhead has no competition in handling 100,000 to 180,000 passengers per hour on two tracks, one express and one local. On the surface, the street car cannot be excelled in the range of 6,000 to 12,000 passengers per hour.

In low-density traffic such as that permitting the use of 40 passenger vehicles with headways of 15 minutes or more (approximately 240 passengers per hour) an advantage can probably be shown for the use of gas busses. But even here, electric transmission with either a gas or Diesel engine may be economically desirable, particularly in frequent stop service.

Between the fields best served by gas busses on one hand and street cars on the other, there is a large field in the vicinity of 2,500 passengers per hour which usually can be best served by trolley coaches.

We may conclude, therefore, that where such facts as Mr. Clardy presents relative to the PCC car are obtained and fairly used in the selection of transit vehicles, the inevitable decision must be in favor of some form of electric drive for transporting more than three quarters of the millions of people who daily use public transportation services.

Reference

1. Application of Modern Electric Vehicles to Urban Transportation, C. M. Davis. AIEE Transactions, volume 57, 1938, January section, pages 57–60.

Modern Electrical Equipment for Industrial Diesel-Electric Locomotives

Discussion and author's closure of paper 42-50 by Lanier Greer, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April Section, pages 229–32.

Robert W. Barrell (General Electric Company, Erie, Pa.): Here are some facts illustrating exactly how the modern electrical equipment described by Mr. Greer's paper together with the modern engine very greatly affected the industrial Diesel-electric locomotive within a short period of time.

In 1935 a 65-ton locomotive power plant consisted of a single 300-horsepower 550-

rpm Diesel engine and generator with auxiliary generator. In 1940 there became available for such a locomotive a power-plant unit consisting of a 190-horsepower 1,800-rpm Diesel engine and a generator developed especially for the purpose. This new engine-generator set was exactly one such as described in this paper. Two of the new sets were used in place of the former single set.

The interesting facts in comparing the new locomotive with the old are shown in Table I.

Table I

	Per Cent
Increase in net horsepower at the wheels	19
Decrease in total engine cost	71
Decrease in total generator cost	38
Decrease in total cost of power plant alone	65
Net decrease in power plant cost when applied to locomotive	61

The net decrease when applied to locomotive is less than the decrease in the total cost of power plant alone, because it was necessary to maintain the locomotive weight at 65 tons. Hence, steel ballast was added to offset the decrease in power plant weight, and the ballast cost has been deducted from the saving.

Now the power plant cost in the old locomotive represented 43 per cent of the total locomotive cost. Hence, it is to be noted that modernization of just the power plant alone brought about a cost reduction of 26 per cent in the over-all locomotive cost. And this was accomplished while improving performance by an increase of 19 per cent in the net power.

The paper mentions several advantages of the modern traction motor with double reduction gearing. From a locomotive builder's viewpoint, one great advantage of such a motor is its flexibility in adapting it to several sizes of locomotives. This is well-illustrated by a motor now in use which is actually being applied to as many as six sizes of locomotives, ranging from 25 to 70 tons. One, two, or four motors are used on a locomotive, depending on the weight. Also, any one of several gear ratios may be selected.

It is very apparent, therefore, that such a motor makes possible a high degree of standardization resulting in many benefits. Not only is there a standardization of the motor and items of control equipment, but also many mechanical parts, such as wheels, axles, and motor suspensions, can be standardized to a large degree.

Apropos to the subject of this paper, some mention can be made of the generator and motor-control equipment for modern industrial Diesel-electric locomotives. Such equipment, while a much smaller portion of the locomotive, nevertheless has played its part in bringing about cost reduction, simplification, and standardization.

One control item, the reverser, can be cited specifically as an illustration. There has been developed a direct-operated type of motor reverser, radically different in design, much simpler and lower in cost than former conventional types of reversers, which has proved to be a complete success for single-motor and two-motor industrial locomotives. An appreciable saving, simplification, and standardization were thus realized over the conventional electropneumatically operated reverser with its master controller.

As stated in this paper concerning the propulsion equipment, it can likewise be said of the control equipment that new materials and methods have been developed and adopted, which have made it possible to reduce weight and cost and, at the same time improve the quality of the product.

An interesting improvement recently incorporated in this type of modern generator, briefly referred to in Mr. Greer's paper, is the engine cranking feature. It is still a common practice to use separate automotive-type geared starting motors with high-speed Diesel engines, but it now has been proved that a cranking winding can be added to the generator with the important three-fold result of reduction in over-all cost, increase in reliability. and simplification.

The idea of using the generator as a cranking motor is old and formerly commonplace. Its application to the modern high-speed equipment is a splendid example of the efforts to produce for industrial Diesel-electric locomotives machinery of the simplest and most rugged design.

Along this same line of thought concerning the development of simple and rugged Diesel-electric equipment for industrial locomotives, it here seems in order to amplify the description of and give facts about the generator excitation and engine control scheme.

A Diesel engine is inherently a constant-speed, constant-power machine. The combination of traction generators and traction motors on a locomotive is not. Thus, the fundamental problem concerning the electrical equipment on a Diesel-electric locomotive is to devise for this combination of dissimilar machines the most suitable scheme for utilizing the power of the engine.

On slow-speed industrial switching locomotives it is obvious that perfect utilization of the engine can be sacrificed in favor of simplicity, ruggedness, and good accelerating characteristics. The excitation scheme described by Mr. Greer does exactly this. Without the use of complicated rotating devices, speed or torque-control schemes, or special windings or separate exciters, a practical method of engine utilization and good locomotive acceleration is accomplished.

This method employs the inherent speed-torque characteristics of a Diesel engine together with the inherent characteristics of a simple shunt generator with but the minor modification of a small amount of separate excitation from the standard cranking battery. The price paid for this extremely simple scheme is very small in terms of loss of power utilization.

Actually the figures on the two commonest industrial locomotive engine sizes are as follows:

> At normal operating generator temperature the maximum reduction in engine speed when operating on the full-throttle torque curve results in 10 per cent reduction in power on one engine and $7^{1}/_{2}$ per cent reduction on the other. However, this maximum power reduction occurs at but one point in the full power utilization range. The actual integrated power reduction realized over only the utilization range is less than 5 per cent for the one and 3 per cent for the other. Since it is not necessary for the utilization to cover the entire speed-tractive effort range of these locomotives, the true integrated power reduction values realized at the wheels are found to be less than 4 per cent and $2^{1}/_{2}$ per cent respectively.

Therefore, it can be said for these industrial Diesel-electric locomotives that the maximum power available at the wheels is but $2^{1}/_{2}$ to 4 per cent less than it would be were a perfect power utilization scheme employed. But it should be observed that this reduction applies only to the possible maximum power obtainable at full throttle. Now in the usual industrial switching service, full throttle is used for but a small fraction of the work, hence the net result of this power reduction when measured in terms of work done is negligible. Surely, this simple and rugged design of modern electrical equipment for industrial Diesel-electric locomotives is well justified.

Lanier Greer: Mr. Barrell's discussion has brought out the fact that the modern electrical equipment for industrial Diesel-electric locomotives has reduced greatly the cost of equipment for these locomotives. Also the mention of ballast being added implies that the weight of the equipment has been reduced. This is a fact and can best be illustrated by comparing the new high-speed equipment with the old low-speed equipment.

In the traction generator this weight-saving can be measured in total pounds of material used per engine horsepower. The modern high-speed generator weighs only 8 to 10 pounds per horsepower against 20 to 21 pounds per horsepower for the old low-speed generator.

For industrial switching, tractive effort is of prime importance. Therefore the continuous tractive effort rating per pound of material used is a measure of weight saving in the traction motor and gearing. The modern high-speed traction motor with double-reduction gearing has a continuous rating of 2.2 pounds tractive effort per pound of material used in the combined motor and gear unit. The older type of traction motor with single-reduction gearing had a continuous rating of only 0.9 pound tractive effort per pound of material used in the combined motor and gearing.

These figures show weight savings of more than two to one when measured by actual ability to do work. Savings of material are always important and are especially so at this time, as all materials used in this equipment are of strategic value to our national security.

Facilities for the Supply of Kilowatts and Kilovars

Discussion and author's closure of paper 42-53 by H. K. Sels and Theodore Seely, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 249–55.

J. W. Butler (General Electric Company, Schenectady, N. Y.): The authors have called particular attention, in conclusion 1, to the effect that the data, on which the study was made, should not form the basis of a generalization, but apply only to the Public Service System. In order that this be further emphasized, and in an attempt to speculate in the general case to allow a generalization, I offer this discussion.

In Figure 3 of the paper the authors give data indicating the annual kilovar load duration requirements of their system. A curve of this general shape can be taken as typical of most systems. With such a kilovar requirement, one may ask: "What is the most economical way of supplying this requirement consistent with good operating practice?"

For purposes of discussion this kilovar requirement will be divided into three sections, as given in Figure 1.

Unregulated Reactive

The major portion of the system base reactive requirement can be supplied by unswitched capacitors. The maximum amount of this type of reactive source that can be used depends primarily on:

1. The light-load voltage conditions in load areas.
2. The system-stability margins during light load.

The light-load voltage problem in the load areas can be sufficiently controlled by judiciously locating capacitors so that this limitation can be sufficiently far removed to the background to bring the other factor to the front. For instance, the closer to the generation the capacitors are placed, the lower will be the voltage rise, and if this problem presents itself, unswitched banks can even be placed on substation busses of four kilovolts or higher for supplying base kilovar requirements.

The next important consideration is, therefore, one of stability. One can readily visualize that if unswitched capacitors are installed in sufficient quantities to require generators to run underexcited during certain periods, instability may arise. Therefore, it is not generally considered good practice to so operate unless intelligent generator voltage regulators are employed. We can, therefore, tentatively set as an upper limit that value of unswitched capacitors which allows the generators to operate at 1.0 power factor during light-load periods. Whether this value can be practically obtained depends upon two factors: transient and steady-state stability.

Transient stability can be very critical to fault-clearing times, for a given kilowatt load and excitation on the generators, and so it follows that if transient stability is a limiting factor, faster switching times allow more unswitched capacitors to be used for the same stability margins. On the other hand, if steady-state stability is the limiting factor, the use of generator voltage regulators will materially increase this type of stability, allowing again more unswitched capacitors to be used for the same stability margins.

Thus, it is seen that with modern relaying and breaker times, and with intelligent generator voltage regulators, this base, or firm system reactive requirement can for all practical puposes be completely supplied by unswitched capacitors. In fact, it may under certain conditions be economical to more than supply this requirement, but this discussion will not go into that refined phase of the problem.

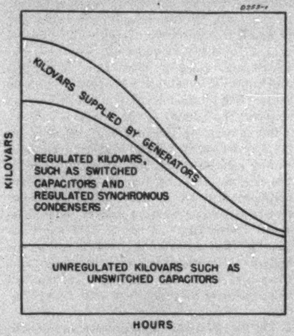


Figure 1. Representative annual kilovar-load duration curve

Kilovars Supplied by Generators

At high-operating power factors it is generally accepted that generators can carry reactive economically. The actual amount that can be carried by the generators in a particular case depends upon the particular generator design and the value and amount of reserve kilowatt capacity, and so, for the sake of discussion, the line of demarcation between the reactive supplied by generators and the regulated reactive will be only approximately located.

Regulated Kilovar Supply

It is in this center region—the regulated reactive requirement—that all engineers are particularly interested, and this is the region in which they will be more so in the future.

I quote from the last paragraph of the section of the paper labeled "Kilovar Supply."

> ". . . . After the limit of static capacitors is reached, if additional reactive capacity cannot be provided economically in new generators or maintained in existing generators because of the value of their kilowatt output, synchronous condensers offer a quick and economic way in which kilowatt generation can be obtained to meet unexpected demands. The higher over-all cost of large synchronous condensers may be partly offset by the convenience of their operation and their stabilizing effects . . . ."

I am concerned as to how the so-called "limit of static capacitors"—as apparently defined in this paper—was arrived at. It is appreciated that the authors' reasoning—and hence their conclusion—in respect to the static capacitor, was made on the basis of the *unswitched* capacitor. But it is not known what factors were responsible in this instance for ruling out the switched capacitor, and so my general findings in this respect will be outlined with the thought that possibly I have overlooked pertinent factors obtaining in the actual operating system, which oversight may be responsible for my arriving at a conclusion somewhat different from the authors.

Let us first consider the economic picture. I have made an economic analysis of the switched capacitor versus the synchronous condenser, based on known factors that can be reasonably evaluated in dollars and cents. Other intangible operating factors will also be discussed. In this analysis, the annual charge for each equipment was determined, and the type of equipment "proving in" was obviously the one having the lower annual charge.

As the equipment losses are the most important factor in such a comparison—where either the condenser or capacitor can fulfill the system requirements—they will be given careful consideration. The fixed, or no-load losses of a synchronous condenser are about 50 per cent of its full-load losses, the remaining loss varying approximately as the square of the load, with full-load losses being from two to three per cent. The losses in a capacitor are a constant, being one third of one per cent of the connected capacity. From this one can see that the total kilowatt-hour loss consumed by the condenser for a given kilovolt-ampere-hour requirement, is a function of the load pattern, while in a capacitor it is independent of the load pattern. Therefore, to make a fair comparison, this must be taken into consideration. So the data in Figure 2 were prepared from standard listed information.

In calculating which equipment would

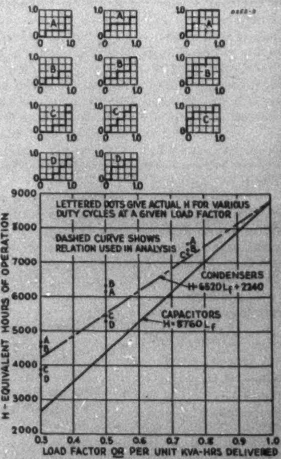


Figure 2. Relation between equivalent hours and load factor L_f under various assumed duty cycles for capacitors and synchronous condensers

have the lowest annual charge, the following basic assumptions were made:

1. Per-unit annual carrying charge on equipment investment taken as 0.125.
2. Annual energy charges on losses figured for data given in Figure 2, for various load factors.
3. Demand charge—per-unit annual carrying charge for use of system investment taken as 0.125.
4. Maintenance and operating charges are taken as equal for each.

On this basis were calculated the data plotted in Figure 3, which show the economic dividing line between 5,000-kva synchronous condensers and capacitors as a function of energy charge in mils per kilowatt-hour, kilovar load factor, and installed cost difference, for two conditions, namely:

1. No evaluation on demand.
2. Capitalized demand charge of $100 per kilowatt.

In equipments of 5,000-kw capacity, a liberal cost differential can be taken as $2 per kilovolt-ampere. On this basis, these calculations show that capacitors prove in, if no demand charge is levied against the peak losses, and only 2.5 mils per kilowatt-hour is made for energy losses, providing the kilovar load factor is not less than 0.3. Since 2.5 mils for energy—and a 0.30 load factor—are conservative figures, it indicates that capacitors in large banks switched for providing regulated reactive are relatively low-priced reactive generators. This, of course, is in substantial agreement with the authors' first conclusion listed under the section "Kilovar Supply," except I believe they were speaking of the unswitched capacitor, close to the load.

Now to consider the differences in system operation obtaining when using switched capacitors or condensers for the regulated reactive supply.

1. *System Stability.* If a system is designed and operated in accordance with well-established principles utilizing modern relaying and switching equipments, the transient-stability problem can be essentially narrowed down and isolated to those cases involving *electrically long lines* and *hydrogenerating stations.* (This excludes the developments associated with high-speed, three-phase and one-phase reclosing for single-circuit lines.) The installation of 5,000-kva synchronous condensers is made—by and large—in subtransmission and distribution areas which are electrically remote from hydrogenerators and long lines. Even adjacent to long lines and hydrogenerators, the character of the condenser in having electrical and mechanical inertia, results in only a second order effect on stability.

2. *Voltage Stabilization.* I feel that the field of the smaller synchronous condenser is practically limited to those cases imposing a difficult regulating job, such as found in substations feeding shovel loads, arc furnaces, and so forth. Under these conditions, the capacitor aggravates the voltage disturbance, if unswitched, and if switched, its switching equipment cannot follow the voltage changes anyway, so for this use it is ruled out. And the synchronous condenser in providing voltage stabilization in this instance holds a strong position in system design.

Summarizing—I feel the use of the switched capacitor as a regulated reactive source will be used more and more in the future, when engineers have accurately studied their system requirements as have these authors, and when more experience has been obtained with the switched capacitor. And as indicated in the paper, as well as in my discussion, each system needs to be studied as a *complete unit,* making it necessary that the distribution engineers, who have been the large user of capacitors so far, fully co-operate with the generation and transmission engineers in designing a system to meet its requirements, namely that of supplying the two commodities, kilowatts and kilovars.

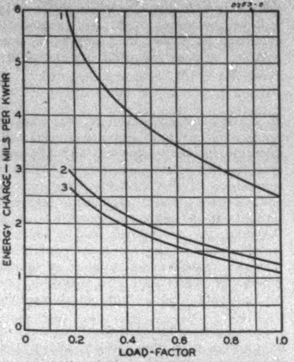


Figure 3. Curves showing economic dividing line between the application of synchronous condensers and capacitors rated 5,000 kva

Capacitors are more economical in regions above each line. Cost difference is capacitors minus condensers

Curve	Cost Difference	Demand Evaluation
1	$4 per kva	0
2	$2 per kva	0
3	$4 per kva	$100 per kw

G. S. Lunge (General Electric Company, Schenectady, N. Y.): It is interesting to note the increasing attention that is being given to reactive power (kilovar) dispatching. Although kilowatt load dispatching is taken for granted in modern utility systems, it is only in relatively recent years that reactive power has been similarly treated from an operating standpoint.[1]

From the manufacturers' standpoint an interesting by-product of this trend is the increasing use of varmeters (reactive kilovolt-ampere meters) for indicating and recording the flow of reactive power. A varmeter gives complete information regarding the reactive power flow, without resorting to any calculations, whereas a power-factor meter reading is of little significance unless considered in combination with the associated wattmeter reading. From an instrument-accuracy standpoint, the use of varmeters is worthy of encouragement. The varmeter is essentially built like a wattmeter and measures the quantity with a high degree of precision, regardless of the load. On the other hand, the power-factor meter, which measures the angle between current and potential, has a high degree of precision only when the load is above one fifth of its current rating. Except for application to synchronous motors, it appears that, in the future, power-factor meters will be used only rarely.

Although the use of telemetered readings of kilowatts has become quite common as an aid to power dispatching, such installations seldom include provision for telemetered readings of reactive power (kilovars). Nevertheless, this paper shows that for maximum utilization of the system facilities, it is essential that the reactive power dispatching be handled as thoroughly as the active power dispatching. The simplest and most effective means for giving the dispatcher a continuous picture of the reactive power flow in the system is to provide telemeter recording receivers in front of him to show:

1. The reactive power output of each major station or substation containing sources of reactive power.
2. The grand total of such readings.
3. The reactive power flow in each tie line interconnecting with neighboring systems.

In certain cases, a few additional kilovar readings from key transmission circuits inside the system itself will be helpful in making maximum utilization of the carrying capacity of those circuits.

When wire line channels are used for telemetering purposes, provision of a reactive power reading in addition to an active power (kilowatt) reading ordinarily doubles the expense of providing such facilities. However, when audio-frequency-modulated carrier channels are used, it is possible to use the same carrier channel for two simultaneous readings, so that the principal additional expense involved in adding the kilovar reading is merely that of the telemeter transmitter and receiver for reactive power (kilovars).

When the carrier channel is of such a nature that it cannot be used without further expense for the transmission of more than one telemeter reading at a time, it is entirely possible to change the telemeter-transmitter potential connections at regular intervals, say, every ten minutes, from wattmeter connection to varmeter connection and vice versa. When the associated (single) receiving instrument is of recording type, there is no difficulty in distinguishing the two records from each other on the same chart, except in the case of tie lines, in which case the use of separate kilowatt- and kilovar-recording instruments is highly desirable because of possible overlapping of the record.

REFERENCE

1. OPERATING ASPECTS OF REACTIVE POWER, **J. Allen Johnson.** AIEE TRANSACTIONS, volume 52, 1933, pages 752–7.

S. B. Crary (General Electric Company, Schenectady, N. Y.): The recommendations listed in the paper by Messrs. Sels and Seely for the improvement in system protection will be recognized as being generally consistent with results obtained from studies made of other systems.

There is a question in regard to the stability calculations on which a little more detailed explanation would be appreciated. The statement is made: "The studies show that the phase-angle displacement is much more dependent on the turbine power output than on the generator power factor." This statement would seem to apply for stable transient operation through a severe fault but would not apply for the case of steady-state pull-out under the condition of carrying a high kilowatt load or a suddenly increased kilowatt load with low excitation. This latter condition has been considered an equally important consideration as a possible limitation for the application of unswitched shunt capacitors to a power system as the transient stability. In fact, this is a more important limitation than the transient case when quick relaying and switching are used. Accordingly, if quick switching and relaying are used with generator voltage regulators, it becomes possible to increase further the stability limitation toward the steady-state case and therefore to allow for a greater ratio between fixed and variable corrective reactive kilovolt-ampere from a stability standpoint. In this connection, also, I would like to ask the authors what is meant in the same paragraph by "pull-out angle." Is this steady-state pull-out angle or transient fictitious pull-out angle, and how is it determined?

E. F. Dissmeyer (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Sels and Mr. Seely have clearly outlined the more important factors which should be considered in connection with determining the ability of a power supply system. An accurate knowledge of the operating limitations of kilowatt- and kilovar-producing equipment is of course essential.

The increasing of power-factor and kilowatt ratings of generators will, of course, result in a reduction in their ability to provide reactive kilovolt-ampere load. It will be found, however, that although many machines are operated at kilowatt loads considerably in excess of their name-plate ratings, they can still provide substantial quantities of reactive kilovolt-amperes.

The excitation requirements at kilowatt loads in excess of the generator rating and at high power factor are usually considerably less than those for name-plate kilowatts and power factor. Therefore, from the standpoint of rotor heating, appreciable reactive kilovolt-amperes is available from a machine which is already carrying a kilowatt load equal to its kilovolt-ampere rating. For many machines, especially those operated at 3,600 rpm, the temperature rise of the stator will be increased only slightly.

For example, a 3,600-rpm, 20,000-kilowatt, 25,000-kva, 0.8 power-factor generator can be operated at 25,000 kilowatts and at 0.9 power factor without increasing the temperature rise of the rotor and with only 110 per cent rated amperes in the stator. Since generators are designed so that they may be operated at 95 per cent rated voltage without excessive heating, the 10 per cent increase in stator current actually represents a five per cent increase in what might be considered a normal operating current. In a 3,600-rpm generator the stator I^2R loss is extremely small compared to the total losses in the machine, and it will be found that this increase in stator amperes will affect the temperature rise of the stator only a slight amount.

In view of the fact that overload operation of machines may be required only during peak load periods or under emergency conditions when other generating units are out of service, it would appear that this type of operation should not cause any noticeable effect upon the life of a generator. In fact, there may be many cases where the rotor as well as the stator might be operated at overloads for rather long lengths of time without seriously affecting the reliability or the life of the unit.

H. K. Sels: With respect to Mr. Lunge's suggestions on the desirability of using telemetering for both kilowatts and kilovars, consideration must be given to the fact that summation of distributed switchable and nonswitchable static capacitors installed throughout the system is impossible, and, therefore, the net kilovar load is indeterminable to a considerable extent. Likewise, where a considerable number of small generating plants feed into a system, the net kilowatt load is not simultaneously available.

In attempting to reach the upper limit of unswitched capacitors which allows the generators to operate at unity power factor during light load as suggested by Mr. Butler, the total generator load power factor probably cannot exceed 95 per cent without certain generators having to operate at unity or leading power factors. However, in general, it is agreed that faster switching times and the use of generator voltage regulators will allow a greater use of unswitched capacitors at light load. With respect to the selection of synchronous condensers in preference to switched capacitors, this question has not been exhausted. The low installation cost of this first group of synchronous condensers in existing buildings was competitive with the cost of switched capacitors located at substations. A portion of the cost of the greater losses in synchronous condensers could be credited to building heating. The voltage-stabilizing characteristics of a synchronous condenser, as compared to a capacitor, came in for some consideration. The initial installation of synchronous condensers offered the possibility of later using the lagging capacity of the machines to regulate the installation of additional unswitched capacitors near their terminals. Some of these may be pertinent factors which Mr. Butler has overlooked. Nevertheless his contribution of a criterion on which to determine the proper proportion of different forms of kilovar supply is welcomed.

The proper proportion of switchable and nonswitchable static condensers, synchronous condensers, and kilovar loading on generators is largely an economic problem, including all factors of capital, operating, and maintenance costs. However, the problem of unstable voltage regulation which may be obtained under all conditions with the use of too much switchable and nonswitchable static condenser capacity must not be overlooked.

In regard to the relative importance of transient and steady-state stability discussed by Mr. Crary, all of the stability studies made to determine the proper kilowatt and kilovar loading of individual generators were based on the transient pull-out angle calculated from a regular a-c network analyzer set up since the steady-state pull-out of the closely coupled Public Service system has not appeared to be a problem. However, it is possible that steady-state conditions should be considered further in the light of Mr. Crary's comments.

As Mr. Dissmeyer points out, there is some temporary overload capacity in most generating units and these so-called short-time proved overload ratings are known, but to use them some sacrifice must be made in stability margins. Undoubtedly, these ratings will be used in extraordinary emergencies, but somewhere the last straw that breaks the camel's back, such as broken blading, is reached, and these ratings cannot be considered as determining the normal system capacity. The use of autotransformers and higher generated voltage firms up these ratings to a considerable extent from the standpoint of thermal and stability limitations. It is believed that full account has been taken of these overloads by planning for only 10 per cent reserve capacity, the lowest in the country, and if these emergency ratings were included in the balance sheet of load and capacity, reserves would probably need to be boosted to as much as 15 per cent. Therefore, no over-all net gain would be effected.

Progress in Design of Electrical Equipment for Large Diesel-Electric Locomotives

Discussion and author's closure of paper 42-23 by G. F. Smith, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 130–1.

R. Tom Sawyer (American Locomotive Company, New York, N. Y.): Mr. Gerald Smith's paper was an excellent paper. We have needed such a paper for some time to sum up the progress in the design of electrical equipment used on Diesel locomotives.

In the early days when the first Diesel-electric locomotives were built, traction motors designed for street cars or other uses were placed under the Diesel-electric locomotive. The insulation was poor, and the ratings very low. Today the traction motor is especially designed for Diesel-electric locomotive service and especially designed for the particular class of service utilized. Today, practically every piece of electrical equipment of the Diesel-electric locomotive is especially designed for its particular service—certainly a very marked step in the advancement of the Diesel-electric locomotive.

I should like to elaborate on Mr. Smith's paragraph on overloading capacity. We must agree that the traction motor of the Diesel-electric locomotive is still the vulnerable spot when it comes to overloading the locomotive. Tonnage ratings are based upon two factors.

1. The rating should be of the proper size in order to maintain the schedule.
2. The rating should stay within the limitations of the traction motors to prevent them from overheating.

For example, the American Locomotive Company and the General Electric Company have one survey department which calculates the performance of their Diesel-electric locomotives and thereby sets a tonnage rating of these locomotives for any given profile. Frequently these calculations have been checked with actual service conditions and have been found to be extremely close. When it is possible to calculate so closely, we find it advisable to not leave any leeway; that is, we expect the railroads to operate the locomotives at the tonnage rating given.

In Mr. Gerald Smith's conclusion he states that a freight locomotive may have twice as many traction motors as a passenger locomotive. This, of course, is generally the case; however, a passenger locomotive geared for 120 miles an hour can be converted into a freight locomotive and made to handle twice the tonnage simply by changing the gearing to 60 miles an hour. The New Haven Railroad is a good example; in this case the locomotives are geared for 80 miles per hour and handle both passenger and high-speed freight service.

G. F. Smith: It may be desirable to explain in detail why more motors are sometimes used on freight locomotives than on passenger locomotives. A 4,500-engine-horsepower locomotive geared for maximum of 80 miles per hour, with worn wheels, for operation on roads with 0.4 per cent (compensated) prevailing upgrades could haul 750-ton trailing load of light passenger cars at balancing speed of approximately 70 to 75 miles per hour, or 1,200 tons of heavy passenger cars at between 60 and 65 miles per hour, or 4,000 tons of freight trains at approximately 25 miles per hour. Such a locomotive would be very useful for passenger and freight service in rolling country with light grades.

If the number of motors is doubled, the locomotive weight would be increased by approximately 40 to 50 tons, and it could also haul freight trains of 4,000 tons on one per cent grades and 2,000 tons on 2.2 per cent grades at approximately half speed.

Doubling the number of motors without change in gear ratio permits operation of passenger and freight trains over a wider range of grades with corresponding reduced speeds on the heavy grades.

A Fast Circuit Breaker

Discussion and author's closure of paper 42-38 by D. I. Bohn and Otto Jensen, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, March section, pages 165–8.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): As a rectifier engineer, I feel greatly indebted to the two authors who were instrumental in developing a new breaker device for protecting mercury-arc rectifier systems.

In 1938 the Allis-Chalmers Manufacturing Company put into operation, at the Alcoa plant of the Aluminum Company of America, ten mercury-arc rectifier units, consisting of twenty 12-anode, 4,580-ampere tanks. These units were protected by standard oil circuit breakers on the primary side, and by high-speed d-c cathode breakers. After a few months of operation extensive tests were carried out to verify the computed characteristics of the circuit and the estimated currents obtained during possible short circuits and backfires. The tests confirmed our computations and also the fact, which was pointed out in the paper, that the increase in current takes place at the rate of over 5,000,000 amperes per second. In spite of the fact that the cathode breakers were able to limit the current in about half a cycle, the current during disturbances would sometimes exceed 50,000 amperes, and, in consequence, greatly stressing all the equipment connected to the system. Shortly after these tests were completed, the customer decided to enlarge this station to double capacity, but it was felt after analyzing the test data, that this high concentration of power should make it necessary to improve the protective means still further.

An investigation was started by our company, along the line of using grid blocking for further protection, and at the same time by Mr. Bohn of the Aluminum Company of America, to develop an anode breaker to be connected between the transformer and the rectifier. It was found that by means of grid protection, the internal short-circuit current in the rectifier, flowing during a backfire (see Figure 2 of the paper) could be interrupted in about one-sixth of a cycle. However, it was found difficult to increase the speed of the large-capacity cathode breaker, which during a backfire has to interrupt the current feeding from the parallel connected units into the faulty anode. In spite of the fact that at that time no suitable d-c breaker was available which could be used as an element of the anode breaker it was decided to develop such a breaker and introduce it into the anode circuits.

Furthermore, it was deemed advisable to equip the rectifier units for this new expansion with high-speed blocking by means of grids, and, in order to reduce the number of breaker elements, to use anode reactors in connection with a six-phase rectifier transformer. The application of anode breakers, and grid-blocking devices as used in this new station, designated as number 4, can be best seen from the paper entitled "Alcoa Rectifier Installations" by Mr. J. E. Housley and Mr. H. Winograd presented at the AIEE Southern District meeting in New Orleans, December 3, 1941.

It can be seen from Figure 2 that as each element of the breaker opens, the anodes are prevented one by one from firing and from feeding into the faulty anode. The protective action of this breaker is, therefore, similar to that action of the grid-blocking device by means of which, as in the first scheme, the anodes are prevented one by one from firing as soon as the grid is made negative. It can, therefore, be seen that both protective devices support each other in the clearing of disturbances which may be very advisable for some installations.

I would like briefly to enumerate some of the points which had to be taken into account when designing this new type of breaker and which are based on experiments made in the first installation referred to above. This will show that we do not here have a 600-volt d-c breaker, as superficial consideration may lead us to believe. From Figure 2 it can be seen that during operation between some of the anodes, a potential double the secondary phase voltage appears, and, therefore, the insulation between poles had to be designed for a voltage of 2,300 volts peak in order to take care of the increase in voltage in the interphase transformer during operation with grid voltage control.

Furthermore, when designing this breaker it had to be taken into account that during normal operation, the current may reach a maximum value of 2,500 amperes in about one fifteenth of a cycle and will drop to zero approximately at the same rate after one third of a cycle. During disturbances, the breaker was supposed to interrupt in about one cycle so that the current could not exceed 50,000 amperes, although it was estimated that the inductance of the circuit would be about one quarter of a millihenry. The tests also showed that such a breaker had to be able to interrupt the above current following a forward current of the magnitude of 25,000 amperes, and that during such interruptions a voltage of 3,000 volts was sometimes exceeded. This voltage had to be taken into account when the breaker was being designed, the reignition of the arc being borne in mind. The maximum interrupting time was set not to exceed one cycle, and it will be seen from Figure 11 that this value was reduced to 0.64 cycle, after further improvements were incorporated by the authors.

We feel, therefore, that a great step forward was made not only in the art of circuit interruption, but still more so in perfecting the protection of rectification systems for large blocks of power.

C. H. Black (General Electric Company, Philadelphia, Pa.) and **Lysle W. Morton** (General Electric Company, Schenectady, N. Y.): This paper is particularly significant to operators of rectifiers and to rectifier application and design engineers. The authors are to be complimented on the paper which treats of the development and design details of an ultrahigh-speed air circuit breaker.

As the rectifier art progressed during the past twenty years, the need for high-speed interrupting devices in the anode circuits gradually became apparent to many of the engineers interested in rectifiers. Desultory attempts were made to solve the problem from time to time, using then available medium-speed or semihigh-speed circuit breakers. However, these did not meet the requirements fully, and it became evident that what was needed was a high-speed multipole breaker equipment designed especially for use with mercury-arc rectifiers.

Some of the general specifications that such a breaker should meet may be listed as follows:

1. The individual poles must be trip-free.

2. Tripping action should be essentially for reverse current. The first and only always completely reliable evidence of occurrence of arc back in the circuit of a rectifier equipment is the reversal of current in the anode connections. Hence a polarized form of reverse-current trip is necessary to distinguish arc-back current from forward overcurrent.

3. High-speed current interruption should be provided both from the standpoint of time required to start reducing the overcurrent and time required to extinguish the arc. This requirement is dictated by several factors. When rectifiers are connected to the same d-c bus with other rectifiers or conversion apparatus, not only do other anodes of the affected rectifier feed reverse current into the faulted anode, but all of the other equipment connected to the bus may contribute current of high magnitude. The value of this current is limited only by the copper resistances of the circuits and the short-circuit impedances of the contributing equipment. For this reason, the rapidly increasing current must be interrupted before damage occurs to apparatus, and before it gets out of hand in magnitude.

In order to provide continuity of operation in larger installations, where several conversion units operate in parallel, the interrupting action should be fast enough to prevent associated equipment from going out of service due to overcurrent, and also prevent "sympathetic" or "simultaneous" arc backs on other rectifiers due to the short-circuit imposed. In many cases, two rectifiers are fed from a single transformer, and the anode circuit breaker, if its action is fast enough, may eliminate the faulty rectifier so quickly that the other rectifier may remain in service. These operational advantages may be obtained if high-speed anode breakers are used, by adjusting overcurrent devices associated with the a-c and d-c switchgear to obtain the proper selectivity.

4. Adequate insulation must be provided between the individual poles of the multipole circuit breakers to take care of the well-known voltages which occur in rectifier transformer circuits.

5. The complete multipole circuit-breaker equipment with its operating mechanism should be simple in construction and require minimum space. It should be so reliable in operation that it does not complicate or increase maintenance of the over-all rectifier installation.

The company with which we are associated is so thoroughly convinced of the real need for and advantages of high-speed anode circuit breakers that we have made available apparatus of this sort. Particularly, for rectifiers whose voltages are above 300, we regularly advocate and apply this type of high-speed switchgear.

One important fact, which we believe should be kept in mind, should be stated here. That is, high-speed switchgear in the anode circuits must not be considered a substitute for adequate a-c switchgear or other protective d-c switchgear. Arc backs are not the only faults which may be encountered with these equipments. In common with all other electrical apparatus, high-quality full-capacity a-c switches are still just as necessary to protect for such things as transformer ground faults, as ever. It is also just as necessary to provide d-c switchgear to protect for overloads and d-c system faults as ever.

D. I. Bohn: Mr. Morton's comments, in which he gave very complete specifications of the requirements of this type of device, need no further enlargement.

With reference to the last paragraph of his comment regarding the desirability of installing a full capacity a-c switch ahead of the rectifier transformer, this is largely a matter of opinion involving type of load, seriousness of infrequent short-time interruptions, and other angles which are essentially economic.

It, therefore, presents a problem apart from the rectifier and associated equipment, this protection being complete with the anode and cathode breakers themselves.

The particular scheme outlined in this paper furnishes adequate protection against transformer ground faults as well as overloads on the d-c system.

High-Capacity Circuit-Breaker Testing Station

Discussion and author's closure of paper 42-26 by J. B. MacNeill and W. B. Batten, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 49–53.

J. R. North (The Commonwealth and Southern Corporation, Jackson, Mich.): We agree wholeheartedly with the authors that methods of testing which merely approximate the breaker rating have very definite limitations. Our experience also has indicated that it is not always satisfactory to extrapolate interrupting data taken at less than rating to determine the full rating.

Circuit breakers may be considered essentially as current-interrupting devices, and it seems particularly important that interrupting devices be tested up to at least their full rating, especially interrupting devices which have a high-ampere interrupting rating. We feel very strongly that the selection and application of circuit-interrupting devices should be made only on the basis of authentic interrupting test data, which clearly demonstrate the ability of the devices to interrupt the duty expected at the actual operating service voltage.

We have been privileged to witness interrupting tests which demonstrated that the interrupting devices as designed and built easily met their rating, and this was subsequently substantiated by actual operating experience. On the other hand, several serious cases of trouble have resulted with devices which had not been adequately tested. For example, it was planned to modernize a group of breakers, and the design of the modernized parts was based on extrapolated data. Interrupting tests on a sample breaker at the proposed service voltage showed the performance to be unsatisfactory and prompted several important design changes. These tests thus proved very advantageous to both the manufacturer and the purchaser.

In another case, breakers which were to be used on service voltage considerably below their rated voltage were found on test to be inadequate to interrupt the higher short-circuit currents at the lower service voltage, although tests at the breaker rating had indicated satisfactory performance. Design changes were made which resulted in satisfactory operation at the lower service voltage and improved operation over the entire application range.

Comprehensive field tests and operating experience are invaluable supplements to factory tests; however, the number of tests which can be made in the field is generally very limited as compared to the number of tests which can be made in a factory test laboratory. Furthermore, in analyzing operating experience it must be remembered that circuit-interrupting devices are generally applied on the basis of short-circuit duty which it is expected will exist in the future, with adequate allowance for system development and growth. Thus for several years after installation a circuit breaker may not be called upon to interrupt currents anywhere near its rating, and the experience indication may therefore be of little value.

J. D. Wood (Roller-Smith Company, Bethlehem, Pa.): I wish to discuss the paper presented by Messrs. MacNeill and Batten describing the additions to their short-circuit testing station making possible the testing of high-capacity circuit interrupters.

Many of us can remember the time when manufacturers had no testing facilities, and design data were accumulated from experience on devices which were installed or an occasional short-circuit test conducted by a utility. These data were sketchy and conflicting, because they were collected under uncontrolled conditions, and the progress of the art was necessarily slow.

With the advent of permanent short-circuit testing facilities by manufacturers has come a period of rapid development. The advance in design and construction of modern circuit breakers is progress of which the engineers of this country can justly be proud.

The high-capacity station described in this paper will be found very useful, not only when testing larger breakers, but also in testing small breakers under conditions comparable with large concentrations of power, as found in present-day utility practice. It should be expected that these testing facilities will result in still further improvements in the apparatus offered for sale.

I would like to take just a moment to describe to you another type of testing station which has been built by one of the smaller manufacturing companies—Roller-Smith. It differs from previous practice in that it is designed to use power directly from a high-capacity bus on a utility property and is adapted for the testing of medium-size circuit interrupters. It is a permanent installation, entirely owned by the manufacturer and operated entirely under his supervision (Figure 1). As will be seen from Figure 1, the station takes power at 66,000 volts over a line approximately a quarter of a mile long, from a high-powered bus in a centralized switching station. With this arrangement voltage is maintained at a high value on the primary of the test transformer. Transformers are provided to step the voltage down to the desired test voltage. A backup breaker is provided to clear all faults, should any trouble develop in the device under test. Reactors with taps are provided to limit the short-circuit current to the desired value. Each of these is in a

Figure 1

separate room, and a third room is provided as a test compartment. A separate building is provided for the oscillograph and other important recording and control devices sufficiently removed to provide safety for all operating personnel.

This station provides testing facilities from 300 volts to 23,000 volts, and up to 250,000 kva. A detailed description has been published in *Electrical World* (January 3, 1942).

I think the advantages of this testing station, located as it is, to subject breakers to the conditions usually found in an actual installation and to do it with a very economical setup, will be apparent to all of you. This station has just recently been completed, and something over 100 short-circuit tests have been made. The equipment for measuring transient voltage conditions is not yet completed. It can be expected that the station, operated in the future, will provide much interesting, useful data.

W. F. Skeats (General Electric Company, Philadelphia, Pa.): Messrs. MacNeill and Batten have done an excellent job of providing in somewhat limited space for a large number and variety of the tests associated with the development of circuit breakers.

Most important among these is the short-circuit testing of circuit breakers approximately to full interrupting capacity. It is unquestionably desirable to be able to determine in the laboratory the interrupting capacity both of conventional circuit breakers and of breakers of new types. This can be done, as Messrs. MacNeill and Batten have done it, by providing sufficient generating capacity to make the test on a conventional circuit. It can also be done with somewhat less generating capacity by the use of a dual power supply, one part of which supplies high current for application during the arcing period, while the other part supplies the recovery transient and the sustained normal frequency voltage. This procedure has been described before the Institute in two previous papers, the discussion of which is quoted as a reference by Messrs. MacNeill and Batten, and further experience with it is mentioned in one of the papers presented earlier this morning.

These papers have shown that the circuits can be switched with sufficient speed and accuracy to realize a transient-recovery voltage rate of several thousand volts per microsecond. Such recovery rates have been read from the oscillogram on both oil and air-blast circuit breakers.

Theoretically, there is some modification of the current wave form as it approaches final zero. But, practically, with most modern breakers, an effective increase of several fold in the capacity of the testing plant can be obtained with a modification which is hardly perceptible. Furthermore, the tests' results may be read from the oscillogram in such a way that any error lies on the side of extra severity of the test, so that the net result is an increase in the breaker's margin of safety. Confirmation of this is given in the three comparisons cited in this morning's paper, which show the synthetic test to be at least as severe as the conventional circuit.

The performance of this circuit is illustrated by the oscillogram shown in Figure 2 of this discussion. In the early part of the film a rather low voltage is shown on the voltage trace, corresponding to the open-circuit voltage of the high-current circuit. This drops to zero when the short circuit is initiated, but, upon interruption, it rises to approximately double its initial value, corresponding to the voltage of the high-voltage circuit, and superimposed upon this is a recovery transient which rises almost to double the instantaneous normal frequency recovery voltage at the time of interruption. It will be observed also that the current wave is not perceptibly distorted.

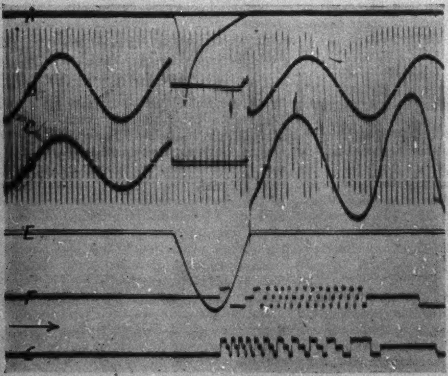


Figure 2. Oscillogram of a synthetic test

6,000/12,000 volts
33,000 amperes
A—Trip-coil current
B—Alternator voltage
C—Oil pressure below baffle
D—Recovery voltage
E—Breaker current
F—Piston travel
G—Contact travel

This circuit has been employed to good advantage in a number of important developments, and its use is expected to continue on the increase.

H. A. P. Langstaff (West Penn Power Company, Philadelphia, Pa.): The authors' description of the high-capacity circuit-breaker testing station at East Pittsburgh seems quite brief and limited to one who has seen the equipment in actual operation. Comparing this with the facilities and surroundings of the Wilmerding tests certainly shows the progress of the Westinghouse organization. Everything pertaining to this testing station seems to be about the last word and this removes practically all of the doubt in an operating man's mind as to the present-day ratings. In discussing this installation with members of our organization, none of us seems to have any suggestions or additional ideas which should be embodied in such a station. The results obtained give the operating men a great deal more confidence in the equipment; in fact, we believe that breakers today are gradually approaching the rating as I outlined at the time of the Wilmerding tests; namely, the standby rating or one such that the operator may stand by the breaker when operating at maximum rating.

W. S. Edsall (Allis-Chalmers Manufacturing Company, Boston, Mass.): In reference to the paper by Messrs. Strang and Skeats on field tests on high-capacity air-blast station-type circuit breakers, and also that by MacNeill and Batten on high-capacity circuit-breaker test stations, it is my opinion that the industry is now faced with the need of a more precise definition of circuit-interrupting factors and conditions, such as it faced almost 25 years ago. Then the famous Hewlett, Mahoney, and Burnham paper delivered here in February 1918, set up the methods of calculating short circuits to enable proper application of circuit breakers for interrupting duty. It pointed out, however, that there were many variables such as transient-recovery voltages and system conditions which could not be taken into account by a general formula, without making all designs so costly as to be economically prohibitive for general use.

Now we have interrupting-capacity testing stations and have a better grasp of these same variables of transient-recovery voltages and system conditions than we had 25 years ago, but the probability of adverse effects from these conditions is greater today, because of the much higher concentration of power to be handled.

The paper by Messrs. Strang and Skeats advocates the single-phase synthetic method of interrupting tests on circuit breakers, while MacNeill and Batten are satisfied with nothing less than a three-phase full-capacity test. Actually, the one great difference between these methods is a question as to the severity of circuit conditions such as transient-recovery voltage. Up to date there is no exact definition or yardstick of transient-recovery voltage against which a commercial circuit breaker must perform on interrupting tests.

In considering plans for extension of our own interrupting testing laboratory, we feel the need of a specification to tell just what we must test for, so far as rate of recovery voltage rise is concerned. It seems that a common basis of test standards and methods must be arrived at. Perhaps as a preliminary step, the best thing to do would be for the General Electric engineers to take their air-blast circuit breaker and test it in the Westinghouse company's laboratory, with the Westinghouse standards of measurement, and for the Westinghouse Company to do the same with their breaker, with the General Electric Company's standards of measurements. I might also add that we might also contribute to such an exchange of breaker and laboratories. The results, I am sure, would make a most fascinating Institute paper, and would give data to establish a sound basis of circuit-breaker testing and application.

J. B. MacNeill: The writers of this paper wish to thank those who have discussed the project, several of whom have visited the laboratory and witnessed tests therein.

A high-power laboratory such as described in this paper requires, in addition to a large first cost, a considerable maintenance and operating expense and occupies space that would be valuable for other purposes. The Westinghouse Company in providing these facilities believes that the investment is justified by improved switchgear and similar electrical equipment resulting from the laboratory operation.

Until the industry grew to a point where this expensive equipment could be absorbed in reasonable costs, it was necessary to rely on occasional tests on operating systems. This was not a satisfactory arrangement for several reasons:

1. Tests could only be made in small numbers and when system conditions provided a minimum of hazard to connected machinery and personnel.

2. The design of circuit-interrupting devices was incomplete, and development tests in large numbers rather than proving tests were required.

3. Available short-circuit concentrations were below the requirements of a growing industry.

The most comprehensive set of field tests ever made on an operating system were those on the Consolidated Gas, Electric Light, and Power Company lines at Baltimore during 1920 and 1921. A total of 200 short circuits in capacities up to 500,000 kva was made on approximately ten different types and sizes of circuit breakers. The cost to manufacturers and operating companies of running the tests probably was not less than $150,000, and the results in improved apparatus were considerable.

Looking back at these tests we realize that the results were in some cases extrapolated incorrectly to meet other conditions for which data were not available. To illustrate: These field tests were all at 25 cycles, and certain conclusions were drawn regarding 60-cycle operation, which were inaccurate. Again, the tests were at 13,000 volts, and subsequent work extrapolated the results to 25 kv with rather disastrous consequences. Again, restored voltage transients were in most cases relatively low and resulted in inaccurate findings.

However, these tests marked a great advance over previous data and the 1,500,000-kva breakers at Hell Gate and other large stations supplied during the 1920's were a result of this work. They have given satisfactory service, and the Hell Gate breakers today are operating without essential change from the time they were installed in 1921.

In contrast today we can under some conditions make 100 tests in an eight-hour shift. Large numbers of tests are necessary, since the variety of conditions which arise with transient troubles on a power system is very great, and the switches must be good for all. On one difficult design we made 3,000 short-circuit tests. This took a period of months, but we were analyzing what went on during $1^{1}/_{2}$ cycles while the breaker operated. The total elapsed time we were studying then for the whole 3,000 tests was only a minute and a quarter.

This explains why the laboratory described in this paper has already been subjected to over 100,000 tests. On the other hand, all the high-power switchgear tests made on all the operating systems in the country over the 20-year period from 1920 to 1940 probably did not exceed 1,500. We see then that the development of modern switchgear is quite impossible without this expensive and rugged tool called "a high-power laboratory."

But you may say: "Even if you have a Laboratory, why does it need to be so large?" The answer is found in the requirements actually being placed before us today for switchgear performance. One customer is changing existing breakers to 3,500,000 kva. Another has just split his system in two to keep within a limitation of 2,500,000 kva.; shortly, he will need larger capacity. A third is now operating with bad voltage regulation, because his switching capacity will not permit adding ties to his system which would increase the interrupting duty. In the not too distant future 3,500,000 kva will be necessary at our more important transmission voltages rather generally. The laboratory here described is the most adequate one in the world to meet these requirements.

Field Tests on High-Capacity Station Circuit Breakers

Discussion of paper 42-12 by H. D. Braley, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 31–5.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): We are all impressed, I am sure, with the magnitude of the undertaking related by Mr. Braley in shorting the Hellgate Bus with 294,000 kva on the test stub and tied in with the rest of Hellgate through Waterside and Niagara-Hudson. This is by far the greatest concentration of power for such purpose ever put together at 13 kv for a field test.

Previous circuit-breaker tests at station voltage have given 25,000 to 30,000 amperes. New York got 65,000 amperes in the arc on one shot. However, the Baltimore tests in 1920, giving 25,000 amperes, resulted in the circuit-breaker design now in use at Hellgate, and these breakers have been satisfactory. It seems reasonable then to assume that these later tests should provide data on which adequate designs for the future can be based.

The paper itself makes clear the precautions taken before the tests and relates certain incidents which show the necessity for care in such a project.

The phraseology is excellent in places and carries plenty of meaning to those of us who have witnessed field tests over a period of years—"The psychological effect of the flash and the accompanying sound effect, in connection with the arc in the test breaker, undoubtedly lent some emphasis to the need for more caution." This could hardly be improved upon. As to the advisability of field tests on circuit breakers, there may be a difference of opinion in some cases. Practically all such tests so far have been worth-while in the development of the art, certainly the Hellgate tests described by Mr. Braley resulted in desirable improvement which, otherwise, might not have been so promptly made. However, as currents increase, the stresses between bus runs, and so on, go up at the current square, and the possibility of damage also increases. Perhaps then it is not reasonable to expect tests on utility systems much beyond those here related.

A particularly valuable type of field testing during the last two or three years has been the work done at 220 kv, some of which has been described to the Institute. There are certain phenomena connected with extremely high-voltage switching such as arc prestriking, restriking tendencies, and characteristics of charging current opening, which are not present in lower-voltage work, and some of which cannot be simulated exactly in laboratories at this time. The knowledge of these factors gained recently in field tests at 220 kv has been a most valuable contribution of that close co-operation between designer and user which is so much in evidence in these papers you are here considering.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The thorough advance planning by those familiar with the details of design, application, construction, and operation, unquestionably played a major part in the success of the short-circuit tests described by the author. Aside from the principal objective of the tests, the discovery of weak points in the system protection, which had unquestionably escaped detection after adequate low-voltage tests, was probably of sufficient value to justify making them.

Careful planning of short-circuit tests[1] reduces the risk of damage to sound equipment to a negligible value. Unforeseen defects disclosed during short-circuit tests are not likely to cause serious system trouble, since a high concentration of specially trained personnel will be on guard at many points. The same unforeseen difficulty, if allowed to persist until an accidental system fault occurs, may result in serious service and equipment troubles.

Material gains are usually associated with appreciable risks, but the very low ratio of risk and benefit which is obtained with short-circuit tests is not so widely appreciated as it deserves. The results of many short-circuit tests made in the past were not published, probably for reasons other than those concerned with the technique and value of the test procedure. It is suggested that the appropriate AIEE subcommittee might wish to compile an informal list (from the information which may be readily collected) of short-circuit tests performed during the past few years, giving names of companies, dates, and a brief statement of the nature of the tests.

REFERENCE

1. TESTING OF HIGH-SPEED DISTANCE RELAYS, E. E. George. AIEE TRANSACTIONS, volume 52, 1933, page 802.

R. L. Webb (Consolidated Edison Company of New York, Inc., New York, N. Y.): With reference to the paper by Mr. Braley describing the setup for field tests on a 15-kv indoor air-blast circuit breaker, it may be of interest to know what recovery voltages were maintained.

Mr. Braley has described the circuit connections in detail. It will be noted that the breaker test house had to be located at a position where it was necessary to use two three-conductor lead-covered cables in parallel for the main test circuit. Each cable was approximately 600 feet long. Also, this connection contained no lumped reactance near the test breaker, since it was desired to obtain the highest short-circuit current that could be provided with the available capacity.

The circuit constants, particularly the cable capacitance, were such that only low recovery-voltage rates could be expected.

Table I of this discussion shows the current in the first phase to open, the approximate short-circuit kilvolt-amperes on a three-phase basis, the calculated recovery-voltage rate and the recovery-voltage rate obtained during the tests with the cathode-ray oscillograph. These are given for the 1941 tests only, though the values obtained in 1940 were of the same order of magnitude. The recovery voltages on all the tests were lower than those expected in an application of these breakers at Sherman Creek station. At this location the feeder breaker recovery-voltage rate has been calculated to have a maximum value of 2,400 volts per microsecond, on 500 megavolt-ampere breakers.

Table I. Field Tests on High-Capacity Station Circuit Breakers Calculated and Actual Test Recovery-Voltage Results

Test	Current Amperes*	Approximate 3-Phase Short Circuit-Kva	Rate of Rise Recovery Voltage Volts Per Microsecond: Calculated	Rate of Rise Recovery Voltage Volts Per Microsecond: Actual Test
1-A	24,000	600,000	270	230
2-A	22,000	580,000	270	250
3-B	47,000	1,200,000	360	310
4-B	47,000	1,180,000	360	390
5-C	62,000	1,560,000	330	290
6-C	58,000	1,480,000	330	250
7-D	26,000	615,000**	210	160

*Initial arc current in first phase to open.

**Line to ground test—kva is equivalent three-phase value.

The maximum recovery-voltage rate expected on 1,500-megavolt-ampere and 2,500-megavolt-ampere main bus and bus-tie breakers, in the same station, is indicated by calculation to be 4,600 volts per microsecond. We, of course, expect that the breakers will handle these recovery rates.

The main test circuits were set up a second time, with no voltage applied, to permit the General Electric Company to apply low voltage tests with their recovery voltage analyzer.[1]

We are informed the results agree substantially with the cathode-ray oscillograph records obtained on the actual tests.

REFERENCE

1. THE RECOVERY-VOLTAGE ANALYZER FOR DETERMINATION OF CIRCUIT RECOVERY CHARACTERISTICS, G. W. Dunlap. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 958–62.

Field Tests on High-Capacity Air-Blast Station-Type Circuit Breakers

Discussion and authors' closure of paper 42-30 by H. E. Strang and W. F. Skeats, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 100–04.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Strang and Skeats outline very well the actual development in the factory and field of a large compressed-air breaker. The design is neat and compact and lends itself well to station layout. The tests referred to are up to the rating of 1,500,000 kva under discussion.

Since breakers of the same type of 2,500,000 kva are being installed shortly in New York, a statement of the bearing of this work on the higher rating would be of interest to designers and users.

The authors discuss the synthetic method at some length as corroborating the results of field tests. Since this method was made public several years ago, I assume it was available before the first series of Hellgate tests. Is this a reasonable assumption, and if so was it used before these tests?

A fair statement of the limitation of the synthetic circuit is made by Strang and Skeats. They dispose of these limitations rather too briefly however. The interpretation of test results referred to in the paper detracts from the value of the device as a demonstration of ratings. Again, Figure 4 of the paper on recovery voltage transients shows three curves, none of which exceeds at any point the normal open circuit voltage. Are we to understand that this is typical of the synthetic circuit?

On the breaker design itself, which in general is an excellent physical structure, one major consideration is the tortuous air path. The compressed air starts on top of the structure, proceeds through relatively small tubes to the interrupters, changes here from vertical flow to generally horizontal flow, and then at the exhaust changes again to vertical flow. This results in loss of air efficiency against a straight air-flow device and no doubt is the reason that recourse has been taken to 250 pounds pressure. It has been found possible to retain the 150 pounds pressure tentatively agreed to by the industry for indoor applications by an improved design of the air passages so that the lower pressure is adequate. A discussion by the authors of this point would be appreciated.

L. R. Ludwig (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors of this paper have devoted considerable attention to methods of circuit-breaker testing, particularly with respect to compressed-air breakers. These devices are comparatively new to American engineers, and consequently special interrupting tests have more than the usual importance in proving the interrupting adequacy of compressed-air designs.

Long time study of the problem of circuit interruption has proved that, to establish the interrupting ability of a particular circuit breaker, the following sequence of breaker and circuit action must be satisfactorily completed:

1. The breaker contacts must part and form an arc.
2. The current in this arc must be equal to the current interrupting rating.
3. Immediately following current zero, recovery voltage must be applied across the breaker contacts as a function of time. The rate of application of recovery voltage must at least equal that of the actual service circuit as modified by the conduction of the breaker itself after current zero.
4. During the application of recovery voltage, the conduction current in the breaker must not be limited by the inadequacy of the test circuit.

The methods of testing the breakers described by the authors are,

1. Tests in the field.
2. Tests using a synthetic circuit of presumably equal severity.

The field tests as described in the paper, "Field Tests on High-Capacity Station-Type Breakers" by H. D. Braley, were made with the breaker set up in a special sandbag structure in the yard so that approximately 600 feet of cable were used to connect the breaker to the station bus. It is recognized, of course, that this type of test setup was essential for safety. Unfortunately, however, the effect of this cable is to greatly modify the circuit voltage-recovery rates in favor of the breaker. Therefore, it appears that the previously mentioned condition 3 for demonstrating interrupting ability is not necessarily met. For example, in actual installation of a 1,500,000-kva breaker a reactor is often used adjacent to it, and the circuit recovery-voltage rate may reach 12,000 volts per microsecond. Even without the reactor the recovery-voltage rate as determined by the generators may reach 3,000 volts per microsecond and possess sufficient energy so that the damping effect of the circuit breaker is negligible. With the breaker and circuit set as described, however, calculations indicate that the circuit voltage-recovery rate would be only 400 volts per microsecond. This is so very different from the rate if the breaker were actually installed in its own cell that one must raise the question seriously as to the complete proof of the breaker performance for all types of circuits.

I would like to ask the authors if they have cathode-ray oscillograms and voltage-recovery rates of the field test conditions and what values of recovery voltage were obtained.

We have deliberately modified circuit recovery-voltage conditions while testing compressed-air breakers and have found considerably more difficulty in building the breaker to satisfactorily interrupt circuits having high recovery-voltage rates.

The synthetic method of testing is a difficult one to control when attempting to meet the four conditions required to prove adequate interrupting ability. Equivalent voltage-recovery transients can only be established in the synthetic circuit so long as the breaker has not just conducted a high current. We have found that the compressed-air breaker differs quantitatively from other types of interrupters in that

1. It is strongly affected by the rate of rise of recovery voltage.
2. It passes many amperes leakage current following normal current zero.

Therefore, the breaker itself can and does modify the recovery transient originally established by the circuit alone, whether it be real or synthetic. In the real circuit, this modification is simply accepted. However, with the synthetic circuit, which does not have comparable impedance, how is one to judge that the breaker is subjected to the equivalent duty of resisting construction? The authors have offered no criterion for isolating the effect of the circuit on the arc space of the breaker. The leakage current increases greatly with the kilovolt-amperes to be interrupted, and, in consequence, a check of synthetic test methods with laboratory tests at say 500,000 kva does not imply that the methods will also check properly at 1,500,000 kva.

The authors state, with respect to Figure 4 of the paper, that one of the cathode-ray oscillograms—which unfortunately is reproduced without time or voltage scales—indicates a failure of the breaker to interrupt. There is no definite fall of voltage to arc-drop value and associated evidence of a continuous arc in the breaker, however. It may be that the greatly modified recovery-voltage transient is simply the result of large conduction current in the breaker. One cannot be certain. "Failure" may or may not have occurred. But if the circuit had been real, no room for doubt would exist.

The authors refer to interruption, in conjunction with the synthetic test circuit, associated with "high recovery rates." They have given no specific rates, however, and further definite data would be appreciated.

The authors' third conclusion calls attention to the close agreement demonstrated between the factory synthetic test and the field interrupting performance. However, the reasonable question regarding the sufficiency of either set of tests, with respect to recovery voltage, still leaves a great deal to be desired before accepting the check as proof of the breakers' performance.

H. V. Nye (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The paper by Strang and Skeats on a breaker of such similar type, voltage, and capacity is interesting, not only because of the method of testing used, but also because of the differences in arcing time and break distance shown by the two tests. One can hardly be blamed for wondering whether the lower arcing times shown in the latter paper are due to the test method employed, or to the higher air pressure used. Certainly my experience leads me to agree with the statement in this paper that a higher pressure does increase the margin of safety in insuring a consistently short arcing time.

In conclusion, it may be interesting to note that my company is at the present time constructing two outdoor air-blast breakers which will be put in service on a 13,800-volt system requiring an interrupting capacity of 1,000,000 kva, although the breakers are insulated for $34^1/_2$ kv. We all hope that the operating experience with the large capacity air-blast breakers described in these papers will bear out the promise of the laboratory tests which have produced them, and warrant a more general acceptance of the compressed-air breaker.

Joseph Slepian (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In spite of the scarcity with which actual test data are given in this paper, some interesting conclusions can be drawn as to the properties of arcs near current zero in actual circuit breakers, and as to the value of the proposed so-called synthetic tests. Concerning the synthetic tests, a conclusion completely opposite to that of the authors' conclusion 3 should be arrived at.

The idea behind synthetic tests is that after current zero the arcing space presents predominantly dielectric properties and only needs to be given a dielectric strength test at sufficiently high voltage to prove its adequacy for bearing normal circuit voltage. The arc space after current zero, however, differs from usual insulating materials in that just before current zero it has been carrying heavy current and has had a very high conductivity. One certainly should expect, therefore, that just after current zero there might still be some conductivity left, and therefore, in carrying out a synthetic test one should be sure that the dielectric-strength testing means has sufficient capacity to really impress voltage on the space in spite of the dielectric leakage current. Everybody knows that an ordinary "shooting box" cannot test leaky insulation.

This point, along with other points of equal importance which are not being repeated in this discussion, has been raised before and was met with the statement that in the circuit breakers being tested by the advocates of the synthetic test, because of a displacement principle, the arc space was occupied by fresh insulating material which had practically zero conductivity or leakage, right up to its dielectric failure point.

But now, we have presented in this paper actual experimental information as to the leakage resistance of the arc space after current zero in an actual circuit breaker of the displacement principle type.

For, by a lucky chance, the circuit used, while not at all a test on the interrupting capacity of the breaker, is just the kind of a circuit which one would devise if one set out with the research problem of measuring the leakage resistance of the arc space after current zero. With the cathode-ray oscillograph the voltage impressed on the arc space by the test circuit is determined; from this the voltage drop on the series resistance of the test circuit and, therefore, the current supplied by the test circuit becomes known; and with voltage and current in the arc space known, its resistance is determined.

So we look at Figure 4 of the paper and particularly curve *C* with great interest. This is said to be for "breaker failing." This conclusion is not justified. The arc space as the oscillogram shows, is quite able to bear full voltage as fast as the synthetic circuit is able to supply it. If it represents the failure of anything, it is the failure, because of insufficient capacity, of the synthetic circuit to give an adequate dielectric-breakdown test on the arc space. But it does very nicely give us information on the leakage resistance of the arc space, after current zero. It shows that under the circumstance of that test, and where, therefore, the breaker was operating satisfactorily in a conventional circuit, presumably on the displacement principle, the arc space resistance begins by being small compared to the series resistance of the test circuit and becomes large compared to this resistance only after three divisions on the time scale are shown. The authors do not give us this time scale, but I guess that the time involved is several hundred microseconds!

Now the series resistance of the test circuit, according to the data given in the paper, was varied from 300 ohms to 3,000 ohms. Hence we conclude that the arc space resistance was of this order of magnitude for a length of time after current zero corresponding to three divisions of the time scale of Figure 4.

But this contradicts the displacement principle!

In Figure 5, the "border-line–synthetic tests" curve marks only the limits of the sensitivity of the synthetic circuit for measuring the resistance of the arc space. It gives no information whatsoever, of the capacity of the breaker for clearing a conventional circuit.

W. S. Edsall: See discussion, page 407–08.

H. E. Strang and **W. F. Skeats:** The discussion of this paper divides itself readily into two parts: that concerning the breaker itself, and that primarily concerned with the synthetic testing circuit. That regarding the breaker will be considered first.

As Mr. Ludwig calculates, the recovery rate in the field tests was comparatively low. Supplementary tests have been made, however, with very much higher recovery rates.

Mr. MacNeill asks about the significance of the field tests with reference to the 2,500,000-kva breaker. In the course of a development such as this, many tests are made, independently varying such factors as pressure, recovery rate, contact speed, shape, number, and location of barriers and coolers, and so forth. When the design of the 1,500,000-kva breaker was arrived at, it was subjected to a large number of synthetic tests to determine its performance under controlled conditions. After the field tests were completed, these results were compared with the factory test results under similar conditions, and it was found that the field performance, if anything, was slightly better than would have been expected, based on the synthetic test background.

The close agreement between the full-capacity field tests and the synthetic tests under similar conditions up to the 1,500,000-kva duty established beyond doubt the validity of this method of testing. The results of the synthetic tests are interpreted at their face value with the knowledge that the test procedure is at least as severe as service conditions and may result in a slightly increased factor of safety.

With this benchmark established, the development of the 2,500,000-kva breaker proceeded on the same basis. As was to be expected, an increase in duty of 67 per cent required some changes in design before satisfactory results were obtained, but the basic principle of determining performance applies to this rating as well as to the smaller sizes.

Before accepting the advantages which the configuration of this breaker gives in station arrangement and design, a careful study was made of the effect, if any, of the so-called "tortuous air paths" leading from the air tank to the contacts and from there to the exhaust. Vague references are frequently made to the effect that bends or restrictions in air passages introduce losses, so that the problem remained one of determining the magnitude of such losses. Obviously they must be confined to two kinds, both of which are susceptible of measurement.

(a) Loss of pressure.
(b) Loss of time.

Figure 1 of this discussion shows schematically the principal functional parts of such an air-blast breaker system: the tank, blast valve, pipe, manifold, and chutes. The chart shows results of actual measurements of pressure at various parts along this system. The lower curve was taken with an early design of blast valve, and shows over 40 per cent drop in the valve itself, compared with about 8 per cent in the "tortuous paths." An improved blast valve gave results shown in the upper curve. The drop across the valve itself is greatly reduced but remains about twice the drop through all the rest of the system. Blast valves are an essential part of any air-blast breaker system, and their design must be such as to reduce this drop to a minimum. However, actual tests have proved conclusively that the drop in pressure resulting from the bends in the air paths is negligible.

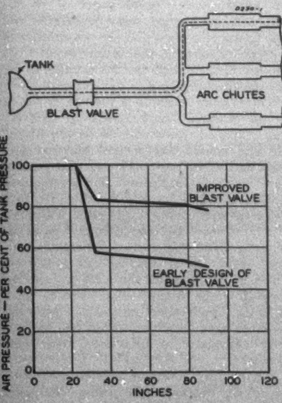


Figure 1. Pressure losses in blast valve and piping

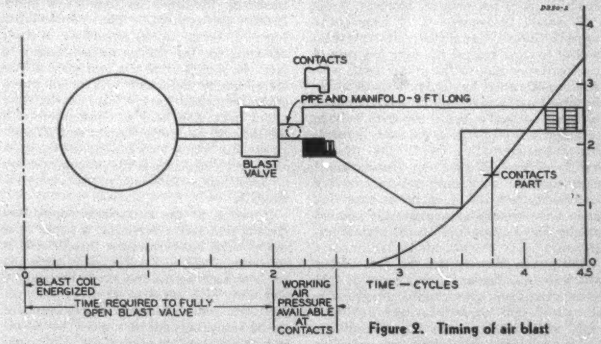

D330-2
CONTACTS
PIPE AND MANIFOLD—9 FT LONG
BLAST VALVE
CONTACTS PART
0
1
2
3
4
4.5
TIME — CYCLES
BLAST COIL ENERGIZED
TIME REQUIRED TO FULLY OPEN BLAST VALVE
WORKING AIR PRESSURE AVAILABLE AT CONTACTS
0
1
2
3
4

Figure 2. Timing of air blast

Figure 2 of this discussion also portrays schematically the principal functional parts, in this case plotted to a time scale. From the blast valve on to the right, reference to the scale below will indicate the time at which normal working air pressure arrives at the various parts. Something in the order of two cycles from the time the trip coil is energized is required in this particular breaker for the blast valve to fully open. Less than a half cycle later full air pressure has arrived at the arcing contacts, after having travelled down the backpipe and along the "tortuous paths" through the header to the contacts. Blade travel is indicated at the right and shows that over a cycle later the contacts part, drawing the arc. In other words, full working air pressure has been available at the contacts in this design for a full cycle before the arc is started.

From such a study, based on actual measurements and not on surmises or speculations, it has been proved that this arrangement is not responsible for any substantial losses either in pressure or time, and that compared with the outstanding advantages which it affords in the way of simple and flexible terminal arrangements, such losses as do occur are unimportant.

A large part of the criticism of the synthetic circuit is based on the effect of leakage current after the final current zero. Up to a certain point the analysis on which this criticism is based is sound. The authors, too, have observed leakage conduction after current zero, and certainly this will affect the recovery transient. Certainly also the resulting difference between the ideal circuit recovery transient and the test transient actually realized will be greater with the relatively high impedance circuit which supplies the recovery voltage in the synthetic circuit which was used than in the low impedance circuit corresponding to field duty at equivalent kilovolt-amperes and recovery rate.

The authors agree, therefore, that it may be very misleading with the circuit shown in the paper to determine the ideal circuit recovery rate for the synthetic circuit, then make tests and, if the tests are successful, assume that the breaker will clear satisfactorily a field circuit with the same kilovolt-amperes and recovery rate. Because of the unsoundness of this procedure, the authors have shunned it, relying instead only upon recovery transients actually observed on the cathode-ray oscillogram taken at the time of the test. Comparing these with unmodified circuit recovery transients to be anticipated in the field is a very much more reliable procedure. It is based on the following logic: Suppose that upon interruption of a given current, a certain test recovery-voltage transient has been obtained on a synthetic laboratory test and recorded on the oscillogram. Now suppose the breaker on which this transient was obtained is to be applied in the field at the same current and on a circuit incapable of producing a recovery-voltage transient which exceeds at any time the test transient recorded on the oscillogram in the laboratory. The application of this circuit recovery transient can certainly generate in the arc path no more heat due to leakage current than is generated by the leakage current accompanying the test transient. The arc path will, therefore, gain dielectric strength at least as rapidly under the field conditions as under the laboratory conditions and, hence, will interrupt just as successfully.

Mr. Ludwig states that there is no definite fall of voltage in curve "*C*" of Figure 4 of the paper, which represents a breaker failure on the synthetic circuit. Apparently he has overlooked the substantially vertical line dropping practically to zero from the "*A*" and "*B*" curves about one millimeter to the right of the voltage axis. This is a part of the "*C*" curve and is the definite fall which Mr. Ludwig could not find.

Figure 3. Recovery transient obtained on synthetic circuit showing overshoot to 2.7 times normal

Even if this definite fall and the rise preceding it were not present, however, curve "*C*" would be considered to represent a breaker failure. Admittedly it represents ability to clear against the very low rate of rise corresponding to the curve itself, with due consideration for its low as well as its high points. It might even represent ability to clear against a circuit recovery-voltage transient a little more severe than is represented by curve "*C*" itself. But that is hardly of interest when the breaker must clear against much more severe recovery transients, and the dangers in assuming more have been quite emphatically pointed out by Mr. Ludwig and Dr. Slepian themselves.

With reference to the question about synthetic tests before the first group of field tests, attention is drawn to the statement in the paper that alterations were inadvertently made in the arcing chamber of the breaker just prior to the field tests and after the synthetic tests at the factory. Under these circumstances, any comparison of the field tests with tests made in the factory would obviously have little significance.

One of the attractive features of the synthetic testing scheme is the flexibility with reference to the recovery-voltage transient that results from separating the high-current circuit and the recovery-voltage circuit. Among other things, this makes possible overshoots not only up to twice the instantaneous value of the normal frequency wave, the highest ordinarily encountered in the field, but far beyond in case that should be required. Figure 3 of this discussion shows an oscillogram on which the overshoot reaches 2.7 times the instantaneous normal frequency value.

It is believed that the synthetic testing procedure will find a field of great usefulness both in proof tests on circuit breakers and, as Dr. Slepian has suggested, in telling us more of the fundamental properties of an arc.

Field Tests and Performance of a High-Speed 138-Kv Air-Blast Circuit Breaker

Discussion and authors' closure of paper 42-9 by Philip Sporn and H. E. Strang, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 1–6.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The Sporn–Strang paper makes an important contribution to our American literature on high-speed oilless circuit interrupters. The double-break form of device tested should be entirely adequate for the ratings discussed and, in fact, the complication of a two-break device seems hardly necessary in view of previously published work. The development under discussion here was presented with limited test values by Rankin and Bennett at the last winter convention, and the present paper supplements the previous one by a considerable increase in test kilovolt-amperes.

However, at the last winter convention Ludwig and Baker presented a paper on a single-break compressed-air breaker which had been tested to 8,900 amperes with 132 kv impressed across the single interrupter and with high rates of recovery voltage. Since this value represents kilovolt-amperes considerably beyond that given by Sporn and Strang, one wonders if the additional complication of two breaks is justified.

The review of circuit-breaker development given in the early part of this paper is of interest, as it shows the considerable effort on the part of one operating company to advance switchgear design. Equally important work was conducted at somewhat earlier dates on other properties. Thus the AIEE paper by Baker and Wilcox presented in January 1930 dealt with field tests with 66 kv across a single-pole unit that uniformly gave two cycles or less of arcing, and, when equipped with a mechanism of normal speed, would give less than eight cycles of breaker operating time. Further results of field tests reported by L. W. Dyer in *Electrical World* for April 19, 1930, gave additional data on the Duquesne Light Company system tests of October 12, 1929, and included tests on the Alabama Power system up to 1,170,000 kva at 110 kv with arcing time well below eight-cycle breaker levels.

An especially important contribution was the Plymouth meeting tests on the Philadelphia Electric Company system of November 1929 in which eight-cycle breaker time was met on 220 kv.

These relatively early tests, including those referred to in the Sporn-Strang paper, have in the past been lifesavers in determining the adequacy of interrupting devices. Of late years, however, field tests have been relatively infrequent, as manufacturers' laboratories have demonstrated their ability to handle the test situation. In fact several important utilities who have been approached on the subject of field tests have not felt the hazard to their systems was justified.

The writer is of the opinion that in some respects factory tests against adequate power form a more difficult basis of approval than do field tests. For instance, the Ludwig-Baker paper reported circuit transient-recovery voltage rates as high as 3,420 volts per microsecond. We know of no field location where similar recovery rates have been had on circuit-breaker tests at high voltage.

The two methods of conducting interrupting tests both have advantages. The factory test can be made conveniently without hazard to a large operating system and in a minimum of time. The field tests on the other hand have a large value in operating experience gained during the tests, which points out possible weaknesses in operating setups under fault conditions as well as demonstrating the device on test. Therefore, for many of us the discussion of system effects given by Sporn and Strang is of particular interest and is conclusive evidence of the desirability of high-speed switching for heavy power concentrations.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): I am sure that all of us are glad to see the progress and development of air-blast breakers, for none of us wants to use oil in electrical equipment, particularly in circuit breakers.

We have used it, it is true, and are still using it, but we just don't want oil circuit breakers.

The paper by Sporn and Strang covers the disadvantage of oil circuit breakers in a very clear and complete manner, and no repetition is necessary.

The data presented in this paper, as well as in the associated papers at this technical session, certainly give encouraged hope that the *great day* is nearing when we can buy at a reasonable price circuit breakers without oil for *all our requirements*.

We utility people want them, the steel mills, mines, and industrial plants want them, and the manufacturers want to build them; and all of us in the electrical business have an investment problem in writing off our oil circuit breaker and associated investments, and manufacturing plant therefore.

I would like to emphasize very strongly the need for continuing with the development of these breakers without oil and would like to show one slide, Figure 1, portraying the unsatisfactory performance of oil circuit breakers during tests to destruction, just to impress upon you the possibilities of a great catastrophe that might result from the use of oil circuit breakers, particularly beyond their capacity.

This Figure 1 shows a picture of an oil circuit breaker being tested to destruction. This oil throw with its accompanying fire and smoke was several hundred feet high and quite wide, as you can readily see. Such an explosion in an operating station would be intolerable.

There were many types and makes of breakers tested by our company about 15 years ago, and this entire test program covered duties up to 500,000 kva at 24 kv. This test program was arranged and carried through quite satisfactorily, even in spite of these spectacular explosions, and the breakers were tested below, at, and beyond their capacities to determine their performance.

I might say, as all of you familiar with oil circuit breakers will realize, that such breakers are not on the market now and you couldn't buy them if you tried. I doubt if any of you have breakers similar

Figure 1. Fire and explosion resulting from breaker failing to interrupt a heavy short circuit

to these (I am not going to tell you what they are), for they have undoubtedly been fixed up so that they are really oil circuit breakers now.

The photo just illustrates something that might happen as a result of an oil circuit breaker attempting to interrupt larger and larger duties due to the system behind them growing. Even with the very best of maintenance and attention, an oil breaker is something we just don't want to keep around.

While these comments are referred to Mr. Sporn's paper, they apply equally to the companion papers on circuit breakers presented at this meeting.

W. S. Edsall (Allis-Chalmers Manufacturing Company, Boston, Mass.): Four years ago the company with which I am connected conducted its first commercial witness interrupting-capacity tests on a 15,000-volt, 250,000-kva air-blast circuit breaker. Three years ago, we made the first installation of a 15-kv, 500,000-kva air-blast circuit breaker. At that time we advocated compressed air for power circuit breakers. Then we were alone in our recommendations of the air-blast principle. Now we have a lot of good company, as is evidenced by the papers presented here today. I congratulate the engineers who have presented papers here at this session on the air-blast circuit breaker they have produced. While the different designs have much merit, the outstanding fact is that we manufacturers are all seemingly in agreement that the air-blast principle is safe, sound, practical, and efficient, as is amply demonstrated by the laboratory and field tests presented today, and by the thousands of interrupting tests made in our own laboratories. It seems we were also in agreement that compressed air is the only medium other than oil which can be applied over a wide scope of interrupting and voltage ratings. Air-blast breakers as manufactured by ourselves and by foreign manufacturers with whom we have been associated for many years are in operation today in this country and in Canada, extending from 100,000 kva at 2,300 volts indoors, up to 2,500,000 kva at 220,000 volts outdoors.

We think that the Sporn and Strang paper on field tests on the 138-kv air-blast circuit breaker is of particular interest, because it is a summation of one manufacturer's efforts to reduce arcing and reclosing time to a minimum. We appreciate the courage with which the American Gas and Electric Company's engineers faced the matter of making interrupting and reclosing tests of such magnitude, upon a system carrying important loads. The tests indicate the ability of the air-blast breaker to do substantially the same job as the oil circuit breakers of equivalent rating. However, the interrupting time of the breaker described seems quite long. In the outdoor air-blast circuit breakers of our associated companies before referred to and now operating in Canada, the 138-kv and the 220-kv breakers now in operation have interrupting times in the order of $3^1/_2$ cycles. This compares to five cycles for the breakers described in the paper by Sporn and Strang. Also, on the 220,000-volt breaker having interrupting time of approximately $3^1/_2$ cycles the reclosing times can be set to be quite low, that is, in the neighborhood of 12 cycles. Reclosing may be done on the single-pole basis if desired, and as has been reported in other discussions before the Institute, reclosing tests on a single-phase basis were conducted by our associated companies some years ago.

A further advantage of the breaker of which I am speaking is that reclosing is not done on exterior isolator contacts with the consequent brilliant visible display, but rather is accomplished within an interrupting chamber under 225-pound pressure. This eliminates the visual display and the possible hazard of attempting to reclose in normal atmospheric pressure by isolator contacts moving comparatively slowly and making circuit in the open air.

The authors' statement that it took 30 years to develop a five-cycle, high-voltage oil circuit breaker, but that the same accomplishment was achieved in two years in the air-blast circuit breaker draws our attention. It is felt that it would be well to acknowledge that several of us manufacturers had resident or visiting engineers in Europe, where these high-voltage air-blast circuit breakers were first developed, and that each one's designs are largely based upon European experience. However, as we have shown in our own case, the European experience and actual practice on many high-voltage breakers already installed in Europe, and in Canada give interrupting speeds not of five cycles, but of three to four cycles.

E. W. Knapp (The Shawinigan Water and Power Company, Montreal, Que., Canada): The authors state that, "the success of this development on 138 kv points encouragingly to the prospect of development at higher voltages, such as 230 kv or even higher." The Shawinigan Water and Power Company have in service at this time two different designs of air-blast circuit breaker operating on a 220-kv system. This company has been interested in the development of this type of circuit breaker for some years and has on a number of occasions made available sections of their power system for primary tests.

One-air blast circuit breaker now in service is quite similar to the 138-kv design discussed in the paper. It is rated as follows:

Voltage rating	220 kv
Current rating	400 amperes
Rupturing capacity	2,000,000 kva
Closing Time	17 cycles (60-cycle basis)
Opening time (contacts part)	$3^1/_2$ to $4^1/_2$ cycles
Arcing time	0.5–1.0 cycle
Air pressure	250 pounds per square inch

Automatic reclosing, single-pole or three-pole

Satisfactory primary tests have been conducted on this unit with one phase to ground, with and without the automatic reclosing feature in service, up to approximately 100,000 kva at 245 kv and up to approximately 1,000,000 kva at 220 kv.

It is noted that the 138-kv air-blast circuit breaker operates on an air pressure of 350 pounds per square inch, as against 250 pounds for the 220-kv air-blast circuit breaker. Have the authors tested the 138-kv unit at pressures below 350 pounds, and may we assume that operating at pressures above 250 pounds on this particular design does materially increase the rupturing capacity or produce other advantages?

The second type 220-kv air-blast circuit breaker now in service on the Shawinigan Water and Power Company system is rated as follows:

Voltage rating	220 kv
Current rating	600 amperes
Rupturing capacity	2,000,000 kva
Closing time	15 cycles (60-cycle basis)
Opening time (contacts part)	$3^1/_2$ cycles
Arcing time	one-half cycle
Air pressure	115 pounds per square inch

Not arranged for automatic reclosing

This design uses the cross-blast principle and is somewhat similar in operation to the type described by L. R. Ludwig, H. M. Wilcox, and B. P. Baker.

A series of primary tests was conducted on this type during the development stage which resulted in a considerable modification of the original design. The unit now in service has been tested on the power system to approximately 900,000 kva at 220 kv. Although the tests appeared to indicate satisfactory performance at the specified ratings, there has been insufficient operating experience to insure satisfactory service under all conditions.

During the primary and secondary tests on air-blast circuit breakers on the Shawinigan Water and Power Company system, a number of conditions were noted which might be of interest. This applies particularly to outdoor service in the rather severe climatic conditions of Canada.

Air-drying facilities and convenient supervision of the same is important.

Special attention may need to be paid to the problem of condensation.

Using the correct materials is important, not only for satisfactory operation, but also for weathering. Machined parts must be carefully chosen and adjusted to prevent sticking under extreme variations of temperature. Air pressures from 100 to 350 pounds per square inch are being used. It would be very desirable to standardize on a closer range of pressures.

Philip Sporn and **H. E. Strang:** It is true, as Mr. MacNeill has stated in his discussion, that improvements in manufacturers' laboratories have increased their ability to handle the testing of circuit breakers. On the other hand, it is our impression that field testing of circuit breakers has not fallen off either in frequency or importance in later years; also some very important utilities have in late years become very much converted to the value if not the necessity of making occasional field tests on their own systems. It is still true, of course, that many other important utilities are not now and never have been willing to subject their systems to staged interrupting capacity tests.

Notwithstanding our gratification on the good results obtained from the test on the air-blast circuit breaker, it is our belief that Mr. Jones has passed sentence rashly and altogether too severely on the oil circuit breaker, considering the present state of development and experience with both oil and air. Without discounting the encouraging and promising results obtained with air, it is still true that the modern oil circuit breaker is doing an outstanding job. Oil fires of anything like the nature indicated by Mr. Jones' photograph are extremely rare for really modern breakers when subjected to duties within their ratings. As a matter of fact, Mr. Jones' discussion lends support to the feeling which has been maintained for some time that factory tests and field tests supplement each other, and each have an important and distinct place in the development of circuit breakers. During the same general test program which Mr. Jones has described, and with the same setup and operating conditions, one breaker equipped with up-to-date arc-control devices which had been developed with the aid of factory test equipment was tested. It successfully passed these tests without any such disturbance as shown in the photograph.

As Mr. Knapp has suggested, the industry undoubtedly should standardize on such matters as air pressure in due time. Development tests which have so far been made have indicated that within certain limits interrupting performance improves with an increase in air pressure. Future designs may make possible the same kind of performance with lower pressures, but, for the time being, it does not appear advisable for the industry to standardize on this factor, since it might result in a handicap rather than a benefit. Pressures of 250 or 350 pounds per square inch are not difficult nor costly to obtain or maintain and represent an economical way of building an increased factor of safety into a line of air-blast circuit breakers.

There is no particular mystery about the design problems in connection with making available a satisfactory air supply. There is no reason to suspect that a straightforward engineering approach with a thorough check of each step will fail to produce results here. At least four years ago an indoor air-blast breaker was mounted in an unheated outdoor switchhouse, together with the necessary compressor and control facilities, and installed during a whole winter in northern Canada. Experience gained during that time has been of great assistance in the design of satisfactory equipment of this kind.

Mr. Edsall has rather jumped at conclusions in assuming that the successful performance reported for outdoor air-blast breakers and high-capacity indoor breakers naturally means that this principle may be extended over the entire range of circuit-breaker ratings. For duty below 500,000 kva, self-contained magnetic-type breakers have been in regular use for several years, and their popularity is steadily gaining.

The test results submitted with this paper include all of the tests which were made in the field without exception. This report was made for the purpose of clearly and completely defining the present state of the art as far as this particular development is concerned. If Mr. Edsall has available results of other tests which show faster operation on some other type of breaker, it is hoped that in the interests of providing the industry with all available information, he will make a similar complete disclosure of all the factors concerning such tests.

As Mr. Edsall has pointed out, our statement in which it was inferred that the entire air-blast circuit breaker development had been accomplished in only two years is not quite correct when interpreted in that way. The authors' comparison of the 30 years of oil circuit-breaker development work to a development of only two years with the air-blast circuit breaker was made primarily with regard to the particular circuit breaker and manufacturer under discussion. However, as pointed out by Mr. Edsall, the statement was probably misleading, particularly if interpreted to include all of the development work done on air-blast circuit breakers. We, therefore, wish to acknowledge the excellent work which has been done over a considerably longer period by other manufacturers including Mr. Edsall's company.

Both Mr. Edsall and Mr. MacNeill have brought up the question of the influence of transient recovery voltage rates on circuit-breaker tests, and Mr. Edsall has suggested that some rule or yardstick covering transient recovery voltage should be set up for testing purposes. While the severity of recovery voltage conditions is important, the bulk of evidence which we have been able to gather to date, with respect to the operation of modern types of circuit interrupters, seems to indicate that these breakers are capable of handling the highest rates that have been encountered. In both field and laboratory tests on modern types of breakers, where conditions have been controlled to obtain both high and low recovery rates, the actual effect on performance as measured by arcing time, appears to be relatively small. For example, in the tests listed in this paper, the recovery voltage rate was varied from less than 200 volts per microsecond, to approximately 2,000 volts per microsecond without any marked change in breaker performance. Furthermore, in connection with a recent survey of voltage recovery rates on power systems, it was found impossible, although an attempt was made, to obtain any definite correlation between recovery rates and breaker distress by studying actual operating records. This does not mean that there have not been any known instances where breakers, particularly of older plain-break designs, have been in trouble under conditions of known high rates of recovery voltage on other systems not included in this survey. It does, however, bear out the general conclusion that most modern breakers are capable of doing their job, regardless of the severity of recovery rate conditions.

In view of all of this, we are not of the opinion that there is anything to be gained by attempting at this time to standardize recovery rate values for circuit-breaker testing. While it is true that voltage recovery rates constitute one of the important factors to be taken into account in the design of circuit breakers, most of the known results to date seem to indicate that this has been done successfully, even for the highest rates encountered. As for a criterion as to what rates may be encountered, the results of the above-mentioned survey of voltage recovery rates made by the Association of Edison Illuminating Companies has already been made available to the circuit-breaker manufacturers, and certainly should constitute a fairly reliable guide for future developments in circuit-breaker design.

A 2,500,000-Kva Compressed-Air Powerhouse Breaker

Discussion and authors' closure of paper 42-41 by L. R. Ludwig, H. M. Wilcox, and B. P. Baker, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 235–41.

R. M. Bennett (General Electric Company, Philadelphia, Pa.): This excellent paper presents a remarkable description of the factors entering into the design of a cross-blast interrupter.

It is of particular interest to note, that although two groups of designers have both employed air-directed across the arc, their solutions to the same problem constitute radically different interrupting devices.

As the authors have stated the problem, and I quote, "A 60,000-ampere arc in an arc chute two inches wide will liberate sufficient gas from the splitters to cause the flow of gas to reverse a distance of six inches against a driving pressure of 150 pounds per square inch." In order to handle these large volumes of gas the authors have placed the arc, as indicated by the sketch on the left in Figure 1, at a point where the throat is very wide, so wide in fact that the operating pressure of the breaker, which is 150 pounds per square inch, can drive away the large volumes of gas that are produced by extremely high currents.

The 15-kv air-blast breaker described by Mr. Braley and Messrs. Strang and Skeats, however, interrupts a 60,000-ampere arc, not in a two-inch throat, but in one approximately half this wide. Air flow through this narrow throat is maintained by a radically different method. As shown in the sketch at the right (Figure 1), an orifice is placed upstream from the arc chute and acts as a flow stabilizing means. In the other view the arc chute would appear to diverge rapidly on the inside, and the orifice configuration would be more apparent. Upstream from the orifice the air pressure is very high since it comes from a storage tank at 250 pounds per square inch. Downstream from the orifice in the region of the arc the pressure is normally very much lower. This positive difference of pressure directly back of the arc chute maintains the flow of air under the most extreme current conditions, because, in order to stop the flow, the pressure in the throat would have to rise to the pressure back of the orifice. The drop in pressure at the orifice also acts as an effective blocking means to prevent particles of metal and carbon from being driven backward where they might impair the insulating properties of the incoming air line.

Under light current conditions, the interruption problem is quite different. The air pressure and velocity required to produce satisfactory interruption is entirely a function of voltage and rate of rise of recovery voltage and depends not at all upon the width of the throat. It is this fact that explains the significant differences between the interrupters. Both of the above designs must maintain virtually the same normal or no-load air pressure at the arc in order to interrupt light currents. But the interrupter shown on the right in Figure 1 employs much smaller air passages than the one at the left, with the inevitable consequence that it consumes substantially less air, in spite of the fact that the air in the tank is at higher pressure. In brief, the interrupter shown at the left permits much of the air to by-pass the arc, while the one at the right with its narrow channel forces air to flow through and across the arc stream.

This fact is reflected throughout the design of the two breakers, in the size of the arc chutes, in the air storage and compressor systems, and, incidentally, in the acoustic shock that results during interruption. The design described by the authors employs three blast valves and very short supply tubes in order economically to deliver large volumes of air to the interrupter. The one at the right employs a single blast valve and supply pipe together with a manifold to deliver air to the three arc chutes. It is this fact that permits the small over-all size and the extreme flexibility of terminal connections that are possible with the latter design.

With reference to the "reversal of flow a distance of six inches," the authors state: "If the rate of rise of recovery voltage of the circuit is slow, the stalled flow will have time to recover velocity, and dielectric will be restored. However, if the recovery rate is fast, the space below the splitters will remain clogged too long, and reignition will result." No matter what theory of operation applies, it must be evident that interruption cannot take place while the interrupter chamber contains incandescent gas. Yet the authors state that this gas is removed after the recovery voltage has started to rise.

What is more remarkable, the removal of six inches of gas, which must be accomplished at least before the recovery voltage reaches its peak value, would require a velocity of air flow of some 5,900 feet per second, or say six times the velocity of sound. This is true if the recovery voltage rises at the relatively slow rate of 500 volts per microsecond. At 5,000 volts per microsecond, it would require a velocity of 59,000 feet per second. It is to be presumed, therefore, that the arc chutes are not so badly clogged with hot gases as the authors believe, and that the majority of hot gas is removed before the current zero.

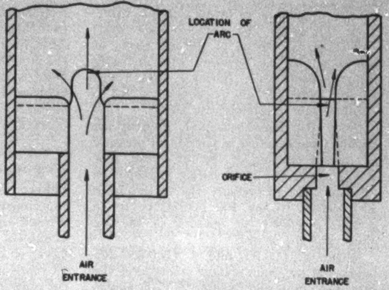


Figure 1. Arc chutes for air-blast circuit breakers

H. V. Nye (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The group of papers submitted this morning on air-blast breakers of large interrupting capacity, both at 15 kv and 138 kv, is very interesting, as demonstrating the claim made in one of these papers that compressed air seems the only practical interrupting medium, except oil, for use on a line of breakers from small to large capacity and throughout the commercial range of operating voltages. This is the conclusion reached several years ago by the engineers of the company with whom I am associated and explains why our developments along the line of oilless breakers have been along the line of the air-blast type.

Some six years ago my company built and tested an outdoor air-blast breaker at 37,000 volts across a single-pole unit. Even with this experimental breaker, operating speeds were very satisfactory, the average time from trip to arc extinction running in the neighborhood of two cycles. Our tests at that time, however, indicated that a great deal of work needed to be done to make the mechanical design of the breaker and its associated parts sufficiently rugged and reliable to make it commercially practicable. Since that time we, as well as other manufacturers, have been working toward the development of thoroughly satisfactory designs. The papers here presented are a good indication that this work has produced results, and it is now practicable to build air-blast breakers for the more usual breaker ratings.

The test results given in the paper by Messrs. Ludwig, Wilcox and Baker are valuable, not only because of the high capacities in kilovolt-amperes interrupted, but also in the wide range of currents and recovery voltages employed. They show a consistency in performance under varying conditions of load and recovery voltage which I think is typical of the compressed-air type of breaker. On the commercial breakers which we are now building up to 500,000 kva we have repeatedly demonstrated this consistency in performance.

On studying this paper I noticed the statement "that the breaker never failed to clear the circuit when the contacts were more than a fraction of an inch apart." As the tabulated data all show a separation of contacts at interruption of anywhere from 1.1 to 3.8 inches on the three-phase tests for the last phase to clear, I would be interested in having the authors explain as to just what meaning they intended to convey by this statement.

In the notes under Table III a statement is made that 58 consecutive tests, as outlined in the tables given, were made without any maintenance on the breaker. This is a record of performance which I think should be particularly mentioned. As an illustration of the fact that smaller types of air-blast breakers are also capable of repeated operations under interrupting duty, I would cite a series of tests recently run on one of our 150,000-kva five-kilovolt air-blast breakers. This unit was rated for a maximum interrupting amperes of 37,500. A series of 97 close–open short-circuit tests was run on this breaker with 25 per cent of the shots at each of the following values—10,000 amperes, 20,000 amperes, 30,000 amperes, and 40,000 amperes. There was no maintenance on the breaker during these tests, and at the end the breaker was in operating condition and capable of taking further shots.

H. A. P. Langstaff (West Penn Power Company, Philadelphia, Pa.): The authors have presented an excellent story on a new development which should prove of great value to the industry. A great deal of theoretical and practical principles have been applied to this compressed-air breaker.

High-capacity and quite complete factory tests, simulating operating conditions, have materially assisted in proving the breaker capable of meeting its guarantees; yet we must apply these breakers in our powerhouse laboratory where any remaining deficiencies will be developed, resulting from factory assembly and so forth.

Comparing the functioning of this compressed-air breaker with that of the ordinary oil circuit breaker and the functioning medium, namely, air versus d-c control voltage, I raise the following question. The tripping of an electrically operated oil circuit breaker has a guaranteed range of 90 to 140 volts, but in the majority of cases it will trip at a much lower value than 90 volts. Its rupturing capacity is not thereby affected, whereas in the case of the compressed-air breaker, it may trip on low pressure, but this, in turn, materially affects its rupturing capacity. I understand control circuits are arranged such that the breaker is now allowed to trip if the air pressure drops below a certain value.

In the paper the authors state that: Enlarging the throat of the arc chute or increasing the air pressure will obviously improve conditions; however, both result in an increased air consumption for a nominal gain in interrupting ability.

Since the kilovolt-ampere interrupting capacity is a function of pounds air pressure at certain opening of the arc chute, how much would the interrupting capacity be affected if the air pressure was reduced from 150 pounds to say 100 pounds or to 75 pounds provided the opening of the arc chute was the same?

Would a 1,000,000 and a 2,500,000-kva breaker of the same voltage rating have the same arc chute and opening? I am thinking of standardization.

R. A. Hentz (Philadelphia Electric Company, Philadelphia, Pa.): This paper describes another stepping stone in the commendable progress that the circuit-breaker designers have made in the march toward adequate oilless switchgear, starting some 15 years ago when the company with which the authors are associated first produced commercially the Westinghouse air De-ion breaker.

The tests described are the first, so far as I am aware, where 2,500,000-kva has been delivered by a laboratory for testing a circuit breaker with the ability to repeat such a severe test as often as desired by those in charge of the test.

One shortcoming is apparent; one, however, that it may be impractical to overcome. These heavy short-circuit tests were made at circuit voltage-recovery rates, but a small fraction of the 12,000 volts per microsecond that may be expected in large generating stations. Such high recovery rates were obtained, but at kilovolt amperes only a fraction of the breaker's rating. Of course, the testing engineer must choose between "his cake or his penny" as the reactor which gives the high-voltage recovery rates cuts down the short-circuit kilovolt amperes. The third from last test given on Table III shows high-current and high-voltage recovery rate occur in what appears to be a more severe combination than any other test recorded. Is it just a coincidence that the arcing time is relatively low?

While the above paragraph points out that the tests do not duplicate the maximum conditions that may be met in certain large generating stations, I wish to pay high tribute to the fact that these tests, so far as I know, approach nearer to such conditions than any other laboratory or field tests ever made.

Progress made in the design of the air-blast circuit breaker gives comfortable assurance that as interrupters they may be relied upon to do what is expected of them, but, if any uneasiness still lurks in the user's mind on that score, it should be offset by the realization that any failure that may remotely occur will not be accompanied by a fire which could spread the damage far beyond the immediate confines of the circuit involved, a consideration of value at any time, but particularly when the possibility of bombings hangs over us.

The description of the compressed-air system in section IV indicates that care and forethought has been given to this important element, and it is well that this has been done, for it is in the air system that most of the troubles may be expected. This is the place where a major number of minor difficulties have already been found in several air-blast breakers already in commercial operation.

L. R. Ludwig, H. M. Wilcox, and B. P. Baker: Mr. Nye has made reference to experimental work and limited use of compressed air in this country for a period of about six years. Compressed-air breakers have, of course, been used in Europe for 12 years or more, and this, naturally, raises the question as to why this form of breaker has not been made commercially available at an earlier date in America. The European breakers have not been called upon to have the high interrupting kilovolt-amperes required for indoor breakers in American practice. This has led to European constructions of the nozzle type, which in general seem to possess a current limitation of the order of 25,000 to 30,000 amperes. At 13 kv this restricts the rating of the breaker to approximately 750,000 kva. In order to extend these limitations, it has been necessary for American designers to develop entirely different forms of interrupters. The cross-blast type, which is capable of reaching 2,500,000 kva, represents a satisfactory answer, but until this type of improvement was made, application of compressed-air breakers in this country was necessarily limited.

Mr. Nye points out that the tabulated data show that the contact separation at the time of interruption reached as much as 3.8 inches and questions this in view of the statement that the breaker never failed to clear the circuit when the contacts were more than a fraction of an inch apart. Detailed examination of the procedure during interruption quickly indicates, however, that these data are not inconsistent. For example, the contacts may part and form an arc just prior to a normal current zero. At the time of the current zero the contacts may be separated only three-eighths inch, and interruption will not take place. Because of the high mechanical opening speed, the contacts can then be 3.8 inches apart at the time of the next current zero. Clearly, the interruption can only take place at normal current zeros. An examination of the data with this point in mind does lead to the conclusion that the breaker always interrupted at the first current zero after the contacts were more than a fraction of an inch apart.

Mr. Bennett has contrasted the interrupting action in two forms of cross-blast compressed-air breakers. With reference to his sketches, it should be considered that the one at the left represents the arc chamber of a 2,500,000-kva breaker, whereas the one at the right shows the arc chamber of a 1,500,000-kva device. Naturally, the 2,500,000-kva arc chamber is wider.

When small currents are being interrupted in the chamber at the left, the air is so directed that it flows straight across the arc and does not diffuse toward the sides, as indicated by the arrows. When large currents are interrupted, the pressure in the region of the arc core (which is central with respect to the arc chamber) develops high counter pressure. During the portion of the half cycle when this counter pressure is sufficient, the air stream is then diverted sidewise as shown by the arrows. The effective action of the air stream at this time is to remove the extraneous ionized gases as they are formed and prevent them from "backing up" into the throat. As current zero is approached, the diversion of the air stream sidewise ceases, and the actual interruption near current zero is quite similar for both high and low currents.

This explanation should make clear the way in which the arc chamber recovers dielectric strength after current zero. It is seen that there is no need to remove six inches of ionized gas after the current zero, since this is done by properly diverting the air stream prior to current zero.

The fourth paragraph of the second section of the paper and Mr. Bennett's remarks in his last two paragraphs refer to the type of arc chamber in which there is no side diversion of the air stream. Mr. Bennett points out that velocities of 5,900 feet per second would be required for interruption, if ionized gas "backs up" six inches. Actually, interruption would probably not occur. The problem can be solved by the use of higher air pressure (250 pounds), which should prevent ionized gases from "backing up" six inches. The authors' solution, however, of "diverting" arc products, permits operation with only 150 pounds pressure.

In the design of the two breakers, the arc chute sizes are reasonably comparable for the same kilovolt-ampere rating. Furthermore, the quantity of air used in cubic feet per interruption, based on atmospheric pressure, is very nearly the same. The air is, however, used in an entirely different manner. The use of mechanically operated blast valves makes it possible to closely time the opening and closing of these devices, so that the duration of the air flow can be made quite short. Therefore, the quantity of air when it is flowing can be made high. The interrupting ability of the compressed-air breaker seems to depend upon the rate of air flow, and, naturally, the higher this rate, the greater the margin of safety. The air-storage tanks in the compressor system can obviously be made of different sizes, but the larger equipment has the advantage of providing the greater number of operations.

Mr. Langstaff has inquired relative to the kilovolt-ampere interrupting capacity of the breaker at lower air pressures. The designs, in general, are based on satisfactory interruption of full kilovolt-ampere rating, starting with tank pressures of 100 to 125 pounds per square inch. The nominal pressure used is 150 pounds. Since the drop in pressure does not exceed 25 pounds per operation, two complete operations are possible without supplying any additional air from the compressor equipment. It has been found that designing on this basis has not handicapped the size and cost of the breaker, because the curve of interrupting capacity versus tank pressure is comparatively flat in the range of 100 to 150 pounds with the type of design used.

Mr. Langstaff has further inquired regarding breakers of various kilovolt-ampere rating for the same voltage service. The arc chute width, the diameter of the blast valve, and the diameter of the air-supply tube are all increased progressively, as the kilovolt-ampere rating is increased. This means, of course, that greater quantities of air are used for interrupting the higher kilovolt-ampere capacities. Pressure is held constant, however, for all kilovolt-ampere ratings, and the design factors are worked out in such a way that standardization should not be a difficult problem.

Mr. Hentz has pointed out that it is difficult to simultaneously obtain high kilovolt-ampere interrupting tests and extremely high recovery voltage rates. As he has stated, we believe the tests shown in Table III of the paper, in which 53,000 amperes were interrupted with a circuit voltage recovery rate of 7,100 volts per microsecond, are the most severe tests ever made. The variation in arc time shown in the entire table is the result

1. Of contacts parting at various points in the half cycle.
2. Of the erosion of the first two splitter plates as the result of the large number of tests previously made on the arc chamber.

Therefore, the short interrupting time of the particular test referred to is to a large extent probability.

Mr. Hentz has pointed out that these tests still do not appear to reach the voltage recovery rates and currents which can be obtained in the field. In this connection, attention is called to the statement in the paper that, "All three of these circuit conditions represent double-frequency transients, but, in the second and third oscillograms, the higher frequency is so greatly damped by the conduction current of the breaker following current zero that little trace of the oscillation appears in the recovery-voltage transient recorded by the oscillograph." This damping out of the higher frequency component indicates that, if the circuit is modified in an attempt to further increase the recovery voltage rate, no actual increase will result, and the interrupting requirements placed on the breaker will not be made essentially more severe than they were during this particular test. If the current is increased, complete damping takes place at lower recovery rates, and, in view of these considerations, it is believed that the tests actually shown do indicate the ability of the breaker to withstand the highest recovery rates which may be encountered, up to full kilovolt-ampere capacity.

Acoustics and the Quiet Train Ride

Discussion and author's closure of paper 42-56 by William A. Jack, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, pages 382–92.

F. C. Lindvall (California Institute of Technology, Pasadena, Calif.): In the development of the new pendulum-type suspension which is incorporated in three passenger cars just put in service on the Santa Fe, Burlington, and Great Northern, the problem of acoustical treatment was given particular consideration. Mounting of the car body on a truck in this new suspension system described in February 1942, *Railway Mechanical Engineer*, permits the use of liberal amounts of rubber in all connections between the truck and car body. This rubber can be operated with quite large static deflections so that transmission of truck noise into the car body through attachments is greatly reduced over what is possible in standard equipment in which the body weight is concentrated at the truck center plates.

Preliminary measurements with two experimental cars showed that the noise level in the vicinity of the trucks was far higher than at other points outside the car body. Consequently particular care was taken with floor acoustical treatment. Calculations made by Dr. V. O. Knudsen showed that a substantial reduction in transmitted noise, about five decibels, could be made by treating the under side of the car in the vicinity of the trucks with absorptive material exposed toward the truck. The theory behind this suggestion is that the intensity of sound in the space bounded by the car floor and the ground can be minimized by preventing sound reflection at the under side of the car. The transmission loss through the floor itself is not much affected by the presence of the exposed acoustical material, but the intensity of sound at the under side of the car is substantially lower than it would be if a hard surface were used under the floor exposed to the truck which is the sound source. Road tests showed that the material known as Airacoustic was mechanically suitable for use under the car floor. Behind this material is a stainless steel sheet which seals the floor heat insulation against moisture penetration.

The car floor is a fairly rigid plywood slab uniformly supported on sponge rubber pads. The floor static deflection is small, but the rubber support is helpful in reducing the transmission of high-frequency disturbances. Comparative tests made with these new cars coupled in with standard modern lightweight equipment indicate a noise level of eight to ten decibels below that of the standard equipment. In the new cars not enough attention was given to acoustical treatment of the air-conditioning system with the result that the noise level, though it be low, is due largely to air noises.

Walter C. Keys (United States Rubber Company, Detroit, Mich.): Apparently noise vibrations travel through rubber and steel to an extent represented by the inverse of the velocities of sound through the two media. In steel, sound travels some 30,000 feet per second; in rubber of 50-durometer hardness (comparable to rubber heels), the velocity is 210 feet per second.

Mr. Jack indicates, in the section entitled "Vibration Isolation," that a bonded rubber isolator used in shear was less efficient than another using a controlled spring in combination with rubber. Experience with the millions of bonded rubber mountings used in modern automobiles indicates that bonded rubber mountings, when properly designed and applied, provide the most practical and efficient known means for isolating both vibration and transmitted noise.

We have a record of an installation of rubber at the center bearing of a six-wheeled truck having performed satisfactorily for 12 years. This is perhaps unusually long service but it illustrates the possibilities where stresses in the rubber are kept low. Insulation at center bearings, side bearings, truck pedestals, draft gears, stems, buffers, pantagraph supports, brake linkage, and so on, are essential to the quiet ride in railroad passenger cars. In addition, all auxiliary equipment such as blowers, motors, and so on should be adequately insulated.

It is well-known that vibrations of any frequency require a definite minimum flexibility to be adequately isolated. Some materials which have been extensively used cannot provide adequate deflections; hence, their value consists chiefly in interrupting metallic continuity.

The writer has too often been disturbed at night by people talking outside of a standing car; this indicates that the "enclosure" effect of the car body could be improved which would lower the noise level within the car.

The remarkable quieting effect of several inches of light snow is well-known. It is hoped that this beneficial effect can be approached or equaled by the use of some inexpensive product which may be easily applied.

A material known as Permacell, capable of sustaining moderate loads for long periods of time could be used as a support for an inner body which could be nonmetallically joined to the outer body. The writer believes that such an assembly can be designed and that it could provide one of several means for reducing the interior noise level.

William A. Jack: Mr. Keys' remarks on the successful use of rubber in several forms are very heartening. The quiet train ride will be obtained by designers, working in conjunction with such experts who know the proper uses of their materials and, in this way, include them in an early stage in the planning at the several points where acoustical experience indicates the noise problems can be attacked. The velocity of sound, as the discussion points out is much lower in rubber than in steel. This fact, in itself however, is not believed responsible for the usefulness of rubber to the acoustical engineer. The velocity of sound in a medium is a function of its elasticity and its density. If the attenuation of energy per unit length were the same, and if the same amounts of energy were introduced into each material, a rubber rod would deliver just as much energy to its far end as would a steel rod, although it would do it more slowly. However, the attenuation of energy per unit length is greater in rubber, and more of the vibrational energy is converted to heat. Furthermore, it is difficult to get large amounts of vibrational energy into rubber in the first place. Pieces of compliant material provide isolation when placed under the feet of a compressor, for example as brought out in the paper. The feet of the machine at the higher-frequency components vibrate much as they did when rigidly mounted. Rubber yields under these small-amplitude high-frequency oscillations in such a way that much of the energy is reflected, not absorbed. The author agrees that if energy does get in the rubber, most of it will be converted into heat before it travels far.

Professor Lindvall's remarks on the application of acoustical principles in the pendulum-type suspension cars are interesting, and the author certainly agrees with the steps taken. The complaint about the air-conditioning system might not have been noticed if the other disturbances had not been so well-reduced. It seems that there is always some smaller noise hiding behind a larger one. Perhaps the conditioning system, in question, has a sufficient run of duct so that Airacoustic may be installed to reduce this noise. As sometimes happens, the trouble may be an air-rush sound from the grilles. This is too far along for the acoustical engineer to be of much help, and the problem lies in the field of redesign.

A New Instrument for Recording Transient Phenomena

Discussion and author's closure of paper 42-57 by S. J. Begun, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 175–7.

E. A. Blomquist (Consolidated Edison Company of New York, Inc., New York, N. Y.): We investigated briefly the possibility of using a magnetic tape recorder for recording transients last August. We found that satisfactory reproduction at 60 cycles was not possible on the outfit we had, which was designed for sound recording. We did no further work on it but referred the matter to the manufacturer.

Mr. Begun's method of getting around the difficulty of recording the low frequencies is very ingenious. I would like to ask what accuracy can be expected from the device, and also how well it will hold its calibration over periods of time.

In applying this device to the recording of power-system transients, it would be desirable to obtain records up to several seconds. As I see it, this would preclude the use of a cathode-ray oscilloscope for viewing the record, due to the limited screen size and the limitations of the persistency of the screen. However, I see no reason why the record could not be taken off on a magnetic oscillograph. I would like to ask Mr. Begun if the recording and reproducing heads and tapes are sufficiently uniform so that a tape containing a record could be taken to the laboratory and there transcribed onto a film using a different head from that with which the record was made. If this were possible, recorders could be set up at various points on the power system, and when a fault condition occurred, the tapes could be removed and brought to the laboratory for transcription.

Another question that comes to mind is the possibility of obtaining more than one record on a tape. In power system studies, six to nine records are usually needed. Possibly this could be done by widening the tape and staggering the recording and reproducing heads.

It seems to me that this method of recording has decided possibilities in the field of power-system measurements, although further development may be necessary before it is practical.

S. J. Begun: It is very gratifying to hear that Mr. Blomquist feels that this method of recording transients has some possibilities in the field of power system measurements. As a matter of fact, during the development stages of the transient analyzer, this application was kept in mind.

The difficulties in recording low frequencies, to which Mr. Blomquist referred, are not surprising, but by the use of the carrier frequency method, very satisfactory results are obtained.

With regard to the problem of how to make the transient visible, the immediate observation, by means of an oscilloscope, is, in very many cases, desirable; but where this cannot be done, because of the length of record required, there is no reason why the signal should not be supplied from the tape to a magnetic oscillograph.

From our observations so far, we feel that the tape, as well as the heads, can be made sufficiently uniform so as to assure consistent results. The tape may be recorded on one instrument and reproduced on another without any difficulty. Furthermore, for all practical purposes, the record life is infinite.

Hot-Spot Winding Temperatures in Self-Cooled Oil-Insulated Transformers

Discussion and authors' closure of paper 42-59 by F. J. Vogel and Paul Narbutovskih, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 133–6.

H. L. Prescott (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): In this paper, the authors have presented some interesting data which add considerably to our knowledge of the variation of gradient and temperature rise with load. The application of these data to operating practices for transformers will, as the authors have shown in Figure 7 of the paper, permit a given transformer to carry considerably higher overloads for short times than has previously been allowed. The authors have shown by their example computation of the temperature at 200 per cent load that to compute the permissible overload for a given transformer under varying operating conditions is a long and tedious job and therefore is not a very practical basis for emergency loading of a transformer.

The winding temperature is determined by the sum of the oil temperature and the winding gradient. The temperature of any other conductor placed in oil and carrying current is determined in the same way. The Westinghouse company has developed a relay, which operates on this principle. The operating element of the relay consists of a bimetal conductor through which is circulated a current proportional to the winding current. The gradient between the bimetal and the oil, therefore, has a definite relation to the gradient between the winding and the oil and, by using the data similar to figures 3 and 4 of the paper, the desirable relation can be accurately predetermined. The bimetal is mounted in the same oil as the winding, so that the oil temperature factor which determines the winding temperature also determines the bimetal temperature. The relay, therefore, takes into account all of the factors included in the sample calculation given in the paper and provides a practical means of operating the transformer to take advantage of its inherent overload capacity. By adjusting the relay so as to obtain a proper relation between the bimetal and the winding gradients, the relay is made to automatically allow higher winding temperatures at higher overloads for shorter times in accordance with an approved time and temperature schedule such as, for example, that shown in Figure 7 of the paper. As the transformer approaches this predetermined schedule, the relay automatically operates an alarm signal to warn of approaching danger and later trips the circuit breaker if the predetermined operating limit is reached to prevent transformer burnout.

L. Wetherill (General Electric Company, Pittsfield, Mass.): This discussion is limited to the circulation of oil in self-cooled oil-insulated core-type transformers. There are several possible assumptions which serve to simplify the subject, and, in general, the broader the assumption the less closely calculations agree with tests. Three assumptions which have been used will be discussed.

Case I. The broadest assumption, and one which has proven adequate for many purposes, is that temperature differences within the oil can be neglected. Oil-temperature rise above ambient- and winding-temperature rise above oil can be calculated independently and added. It is particularly helpful in analyzing transient thermal per-

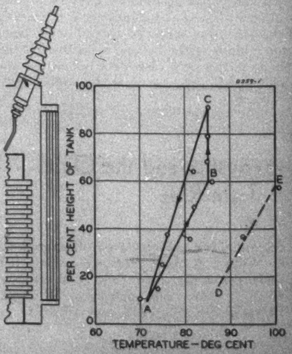


Figure 1. Thermal conditions in 2,500-kva self-cooled transformer

Steady state

——— Oil temperature

— — Copper temperature

o o o Test points

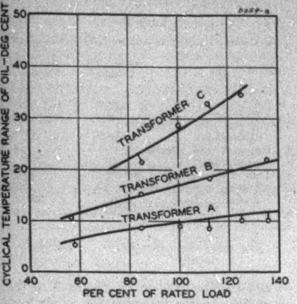


Figure 2. Comparison of design calculations and test results

Steady state
——— Calculated characteristic
o o o Test points

formance, since the resulting equations may be made identical with those representing transient performance in electrical circuits when current is abruptly changed in a circuit containing resistance and capacitance in parallel.

As a result, we obtain thermal transients with exponential decrement which can be superposed on the steady-state condition. The authors have made use of this procedure in analyzing transient behavior, and this procedure is in fact the most satisfactory which has been described in published literature.

Case II. It has long been known that calculation according to the foregoing procedure may lead to errors. Three such cases are as follows:

(*a*). **Transformers in which the core is nearly as high as the tank.**

(*b*). **Transformers in which there is appreciable loss generated above the top of the core.**

(*c*). **Transformers undergoing heavy short-time overloads.**

To predict with reasonable accuracy the higher temperatures which may result from these conditions, the assumption that temperature differences within the oil can be neglected should be replaced by the more accurate assumption that the core and coils can be represented by a single channel in which heat is absorbed as the oil moves upward, and that the tank cooling surfaces can be represented by a single channel in which heat is dissipated as the oil moves downward.

Figure 1 of this discussion shows ultimate steady-state temperatures observed in the oil of a 2,500-kva transformer at 135 per cent rated load. Starting at *A* (in Figure 1), the oil moves upward through the windings increasing in temperature at an approximately uniform rate to *B*. The oil then moves on to *C* at the top of the tank with no appreciable change in temperature. From *C* the oil moves down through the cooling tubes, or past the cooling surfaces, decreasing in temperature at an approximately uniform rate, to *A*; completing the cycle.

The temperature of the copper is shown by the curve *DE* in Figure 1 as a function of the height above the bottom of the tank. It is assumed that the temperature rise above adjacent oil is the same for all parts of the winding. Actually, this is frequently incorrect on specific transformers, but it serves to illustrate the nature of the phenomenon.

If the watts per square inch of winding surface is low, and the copper is at nearly the same temperature as the adjacent oil, reference to Figure 1 will show that the average copper temperature may well be lower than the top oil temperature. This doubtless accounts for the so-called negative gradients referred to in the paper.

The pressure difference which maintains the continuous circulation of the oil is the weight of the column of downward moving oil minus the weight of the column of upward moving oil. The magnitude of the pressure difference is

$$F=\rho\alpha a$$

where

F = pounds per square inch
ρ = density of oil at 0 degrees centigrade in pounds per cubic inch
α = thermal coefficient of expansion of oil per degree centigrade
a = area of oil circulation curve in inch-degrees centigrade

When, as in Figure 1, the oil circulation curve is approximately triangular the area is

$$a=RH$$

where

R = cyclical temperature range of oil in degrees centigrade (temperature at B minus temperature at A)
H = height of center of gravity of cooling surface above center of gravity of generated loss in inches

If the center of gravity of the generated loss is abnormally elevated, due to a tall core in proportion to the tank, or to appreciable auxiliary losses generated above the top of the core, the circulating force is reduced, the rate of oil circulation is reduced, the cyclical temperature range of the oil is increased, the top oil temperature is increased, and the hottest copper temperature is increased.

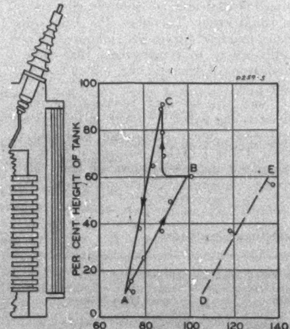


Figure 3. Thermal conditions in 2,500-kva self-cooled transformer

Short-time overload
——— Oil Temperature
— — Copper temperature
o o o Test points

If the resistance to oil circulation is known, it is possible to calculate the cyclical temperature range of the oil. A comparison of such calculations with test results is shown for a number of cases in Figure 2 of this discussion.

Figure 3 shows data corresponding to Figure 1 for a transformer undergoing short-time overload. Oil moving upward through the transformer winding from *A* to *B* rises in temperature and is discharged into the region above the core and coils at a temperature considerably above that of the oil already occupying this region. The streams of hot oil become dispersed and serve to bring about a slow increase in the temperature of the mass of oil between the core and the tank cover. Oil then moves down, from *C* to *A*, through the cooling tubes, completing the cycle.

Therefore, temperature of top oil is likely to be misleading when used in determining hot-spot temperature before thermal conditions have become constant. Temperature of oil in the cooling duct adjacent to the coil furnishes a better basis.

Case III. There are conditions for which the assumption of a single oil path is too simple to give results in reasonable agreement with test data. Such cases fall in the following categories:

(*a*). **Transformers in which one winding is exposed much more freely to oil than another winding.**

(*b*). **Transformers in which a part of the heat dissipating surface is exposed much more freely to oil circulation than another part.**

For these cases it is necessary to represent the transformer by a network of oil channels. For each oil channel the rate of oil flow is governed by the following equation:

$$P=rG+\rho V(1-\alpha T)-\frac{\rho\alpha M}{105G}$$

where

P = pressure at inlet minus pressure at outlet in pounds per square inch
r = flow resistance of channel in pounds per square inch per gallon per minute
G = flow through channel in gallons per minute
ρ = density of oil at 0 degrees centigrade in pounds per cubic inch
V = height of outlet minus height of inlet in inches
α = thermal coefficient of expansion of oil per degree centigrade
T = temperature of oil entering channel in degrees centigrade
M = moment of loss absorbed or dissipated by oil about horizontal axis through outlet in watt-inches

Performance of a network of channels can be determined from the foregoing equation by a process analogous to the application of Kirchhoff's laws.

Evolution of the concept of thermal behavior of oil in transformers requires creation of new terms. For many years the term "top oil rise" was used as though it were the only quantity necessary to specify the thermal condition of the oil. In recent years we have added the term "effective oil rise," and we have recognized the distinction between top oil rise and effective oil rise. A thorough analysis of thermal behavior of transformer oil would involve recognition of at least four different oil temperature rises as follows:

(*a*). "Effective dissipating oil rise" is the average rise of the moving oil adjacent to the tank dissipating surfaces. The "effective dissipating oil rise" is the dominant factor in the steady-state thermal performance of oil-insulated self-cooled transformers, and it can be calculated with a high degree of accuracy.

(*b*). "Effective absorbing oil rise" is the average rise of the moving oil adjacent to the coil dissipating surfaces. It is not necessarily the same for all windings. In normal transformers the "effective absorbing oil rise" is not appreciably different from the "effective dissipating oil rise" and they can be frequently assumed equal.

(*c*). "Effective thermal oil rise" is the average rise of all the oil. It determines loss absorbed by the oil under transient conditions.

(*d*). "Top oil rise" is the rise of the mass of hot oil at the top of the transformer.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): This paper is a very timely one and one that is of particular interest to me, because I have spent considerable time during the past year in obtaining data on hot-spot temperatures in oil-immersed transformers. Altogether about 50 heat runs have been made on three different transformer designs, each under a different condition such as self-cooled and forced air-cooled for both ultimate conditions and for short-time heavy overloads.

The authors show in Table I the gradient of "average copper to top oil." This leads, in some cases, to the assumption of negative coil gradients, a condition that cannot exist when the coils are dissipating heat. Average winding rise should, of course, be based over average oil temperature (not over top oil or maximum oil temperature) and hot spots in coils near the top oil should either be based over top oil or adjacent oil temperature.

The authors also show in Figure 6 that the difference in degrees between the hot-spot and the average winding temperature remains constant for loads ranging from 100 to 250 per cent. My tests show that this difference always increased with load, and under some design conditions it increased as the loss raised to a power ranging from 0.7 to 0.8, particularly under short-time overload conditions.

I would like to ask the authors how the losses of the 600-kva transformer were supplied to the oil. If the losses were supplied in a manner that caused the center of heat to change with load with respect to the center of the cooling tubes, this would affect the hot-spot rise over the average winding temperature and could easily account for some of the discrepancies between our results.

The authors used the method of calculating hot-spot temperatures given in my 1930 AIEE paper, "Loading Transformers by Temperature."[1] While not rigorously correct under some conditions, as pointed out by Mr. Wetherill, recent tests show that this is still the most practical method for obtaining reasonably accurate results.

I cannot agree with the statement made by the authors that the results of their tests make it possible to recommend much higher emergency overloads than those given in the American Standards Association Guides for Operation of Transformers. In the first place, they apparently did not use their test data on short-time overloads in their calculations of short-time overloads. In the second place, they used the same method of calculating overloads (as used in calculating the present ASA curves), except for the expedient of not making any correction for increased losses with temperature, since they assumed that the change in oil viscosity counteracted the increased loss. The effect of this however is small—being in the order of three to four per cent of overload values obtained.

The larger overload values proposed in the paper are mostly due to:

1. Using higher temperature limits than those used in calculating the ASA curves as for example 125 degrees centigrade instead of 115 degrees centigrade, for two-hour overloads.

2. Using 61 degrees centigrade hot-spot rise and 50 degrees centigrade top oil rise, instead of 65 degrees centigrade hot-spot rise and 45 degrees centigrade top oil rise as used in preparing the ASA curves. The difference obtained in the overloads when using 11 degrees centigrade, instead of 20 degrees centigrade, hot-spot rise over top oil (at rated load) is quite large.

While there are many small low-voltage transformers that meet the 11-degree hot-spot rise over top oil, there are also many large high-voltage transformers that approach a 20-degree hot-spot rise over top oil at rated load. Therefore, the ASA emergency overload curves were prepared for and apply safely to transformers that have a 20-degree hot-spot rise over top oil temperature.

Using the hot-spot temperature limits given in the paper, although in my opinion somewhat higher limits can probably be used, Table I of this discussion gives the calculated overload values of large high-voltage transformers in comparison with the overload values shown in Figure 7 of the paper apparently intended for small low-voltage transformers.

Table I. Emergency Short-Time Overloads

Time in Hours	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current Following			
		Small Low Voltage		Large High Voltage	
		No Load	Full Load	No Load	Full Load
1/4	140	3.0*	2.35*	2.4	2.0
1/2	135	2.6	2.10	2.20	1.80
1	130	2.25	1.90	1.95	1.65
2	125	1.9	1.71	1.65	1.45
4	120	1.55	1.45	1.4	1.32
8	115	1.33	1.33	1.25	1.23
24	110	1.2	1.2	1.16	1.16

*Values given in paper.

Table II

	Small Low Voltage	Large High Voltage
1. Loss ratio	2:1	3:1
2. Top oil rise (degrees centigrade)	50	45
3. Hot-spot rise (degrees centigrade)	61	65
4. Time constant	4	3
5. Ambient (degrees centigrade)	30	30
6. Hot-spot rise over top oil and top oil rise over ambient vary as $\text{loss}^{0.8}$		

The characteristics applying under rated load conditions are shown in Table II.

For intermediate classes of transformers intermediate overloads would of course apply.

It will be noted that the overload values for the large transformers are in general appreciably lower. This shows that different overload values should be used for different classes of transformers.

While I do not claim that one can calculate the exact amount of life taken out of a transformer during an overload period by the eight-degree rule, I feel that it is a useful tool—one that enables us to make progress. This suggests the consideration of even greater overloads than those shown above in cases where interruption of service cannot be allowed.

During the war period there may be cases where it would be advisable to take out of a transformer considerably more life than that used up for the overloads shown in Table I, to prevent a shutdown in a war plant or a station supplying power for war work. In fact a munitions manufacturer recently wished to purchase a spare bank of transformers of *minimum size* to carry the load during a period of one or two weeks in case a saboteur put his present bank out of commission. He was willing to sacrifice 100 per cent of the transformer's life.

I believe, therefore, that it is time for us to start studying the question of allowable overloads that will take out various amounts of the transformer's life. While one user may be willing to sacrifice say only one per cent of life during an emergency period, another user may be willing to sacrifice 10, 25, or 50 per cent and still another as mentioned above may be willing to sacrifice 100 per cent of the transformer's life to prevent a shutdown. Of course, caution would have to be used in applying such overloads to transformers already built, since there may be limitations other than thermal in such units.

REFERENCE

1. LOADING TRANSFORMERS BY TEMPERATURE, V. M. Montsinger. AIEE TRANSACTIONS, volume 49, 1930, pages 776–90.

H. W. Hartzell (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper has been read with considerable interest, because every contribution to the knowledge of loads that transformers may safely carry is especially valuable at this time when installed equipment may be called upon during emergencies to serve loads greatly in excess of name-plate ratings.

Continuity of service, prevention of costly shutdowns, and conservation of transformer equipment are some of the items which depend directly upon intelligent transformer application. With many loads growing rapidly and new loads being connected to power systems every day, it would seem that much thought should be given to the overloads that transformers can actually carry under the various conditions of operation.

Table III. Calculated Life for Transformers Operating Continuously at Hot-Spot Temperatures Indicated

Hot-Spot Temperature (Degrees Centigrade)	Calculated Insulation Life (Days)	
	Open Type	Closed Type
140	21	107
135	33	168
130	51	260
125	78	398
120	123	628
115	192	980
110	297	1,520

The authors have presented a table of suggested times and associated hot-spot temperatures which determine emergency overloads that might be applied to a transformer. Although these are of value, the overload ability of a transformer for longer lengths of time is of considerable interest. Momentary overloads occurring during system switching operations would probably be carried without too much regard to the transformer life consumption due to high overloads. The emergency ability of transformers for overloads having a duration of several daily load cycles warrants considerable attention, since under these conditions a substantial amount of transformer life may be consumed.

It would appear that the values given in this paper, although allowing greater loads than the proposed operating guide of transformer standards ASA-*C*57, may be extremely conservative. To illustrate this point Table III has been made from calculations which follow those of Nichols[1] for open-type transformers and Montsinger[2] for closed-type transformers.

With a hot-spot temperature of 130 degrees centigrade Table III indicates that the total life of the insulation in an open-type transformer would be $51\times24=1{,}224$ hours. If the transformer sustained two hours of operation per year when the hot-spot temperature remained at 130 degrees centigrade, this condition recurring annually for say 30 years, the total operation at this temperature would be 60 hours or $60/1{,}224\times100=4.9$ per cent of the calculated life of an open-type transformer operating at this temperature. For a closed-type transformer the life consumption would be $60/6{,}240\times100=0.97$ per cent, or practically a negligible amount for so many emergency operations. If calculations were made for the transformer insulation life loss during emergency loads limited by the temperatures and times proposed in this paper, since these temperatures would be attained only at the end of the time shown instead of continuing throughout, the loss of life would be much less than that indicated in the above illustration even though the unusually large number of emergency operations during the life of the transformer is considered. It would seem, therefore, that greater loads than those suggested in this paper should be considered for transformer emergency operations.

REFERENCES

1. EFFECT OF OVERLOADS ON TRANSFORMER LIFE, **L. C. Nichols.** AIEE TRANSACTIONS, volume 53, 1934, December section, pages 1616–21.

2. LOADING TRANSFORMERS BY TEMPERATURE, **V. M. Montsinger.** AIEE TRANSACTIONS, volume 49, 1930, pages 776–90.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The general method of determining permissible overloads outlined in this paper is correct, for it is a method which has been in common use for the past few years. However, the application of the method by the authors contains serious inaccuracies.

Nontypical transformers, having unusually low temperature rises were selected as a source of data. Values of temperature gradient even lower than those shown by these data were assumed and used for calculating allowable overloads. Consequently, the conclusions drawn are not conservative as claimed.

A brief summary of the most serious errors made in this paper follows: The examples in Table I of the paper are not typical standard 55-degree transformers, since all these transformers have temperature rises of less than 35 degrees centigrade. Any transformer user will testify that seldom, if ever, has he had a standard 55-degree power transformer which had a tested temperature rise of less than 35 degrees centigrade. Consequently, the data used as a basis are not data on typical standard transformers, but on transformers which contain more material than standard transformers and are, consequently, more expensive than standard transformers.

Even the data so obtained, however, were not correctly applied, but lower values of gradient between the copper and oil temperatures were assumed than were obtained on these special transformers. In the tabulation of test given in the paper, two of the measured gradients were above eight degrees at full load; these, however, are disregarded and a five-degree gradient is assumed for determining allowable overloads. A similar liberty was taken in determining the hot-spot correction. In the words of the authors, "On the average, the difference between the hottest copper and the average copper was eight degrees." Immediately following, the data are disregarded, and the authors state: "It appears that actual hottest spot temperatures are in the order of six degrees or less above the average copper temperatures." This figure of six degrees is used in calculating the overloads instead of the eight-degree figure shown by their data.

While these differences are small in degrees, they are large in percentage and, consequently, they make a large error in the values obtained for allowable overloads.

The authors' claim in the conclusion that a more accurate method than previously used for calculating temperature rises has been derived is inaccurate, since the method used has been published before. To name one specific instance, this method was used in the December 1939 issue of the Allis-Chalmers *Electrical Review* in an article entitled "Simple Method of Calculating Transformer Temperature Rise Under Variable Loads."

Because of these fundamental errors, in applying the method, the conclusions reached in the paper are not conservative as claimed.

F. J. Vogel and **Paul Narbutovskih:** Mr. Sealey, in his discussion of our paper, has severely criticized it on the basis of the data furnished and the methods used. Typical of his criticism is his statement that "nontypical transformers having unusually low temperature rises were selected as a source of data." It was not claimed or intended that these were typical of commercial practice, but they were selected because the data for these transformers were available and were used to show that the methods of calculations commonly used are in error. Contrary to Mr. Sealey's further discussion, test data indicate that the conclusions reached in the paper are conservative, and there is a definite possibility that similar values will be recommended for general use.

Mr. Wetherill furnishes some very interesting data in his discussion regarding the thermal behavior of transformers. His analysis of the various cases is good, even though one might wish that the experimental data were given more completely. The fundamental considerations underlying the theory of his first case are essentially in line with those given in the paper. The data given on Figure 2 of the discussion were very interesting to us. The data furnished for transformer *A* seem to be a confirmation of our results. The results given for transformers *B* and *C* do not furnish as good a confirmation, but it is to be noted, that the actual oil temperatures are not given, and so there is no way of comparing the results with the theory outlined in our paper on the effect of viscosity. However, in both cases of transformers *B* and *C*, the variation of the difference between the top oil and the average oil over the range of loads from 100 per cent to 140 per cent load is not over four or five degrees centigrade. It also appears to us that transformers *B* and *C* are not entirely typical. Particularly in the case of transformer *C*, there might be a difference between the average oil and the top oil of nearly 15 degrees at 100 per cent load, and this in turn would lead to the transformer having a hot spot at least 15 degrees above its average temperature, which is not in conformance with the present standards.

In regard to the effect shown on Figure 3 of the discussion it is somewhat difficult to understand why this effect will appear on short-time overloads only. One is likely to encounter this effect when the oil circulation is partially impeded around one of the windings, such as an internal winding of a core-type transformer with a narrow vertical duct. But in this case the effect will appear in varying magnitude at all loads. The difference between the temperature of oil issuing upward from the vertical ducts and the tank oil elsewhere on the same level would be a variable quantity, depending on the magnitude of the load and a change in the oil flow as affected by the pressure difference and the friction head, including the effect of viscosity. A practical way of taking care of this phenomenon in computing the winding temperatures is to treat it as a component of the hot-spot gradient, although it will not vary quite as much as the 0.8 power of the losses. It may be estimated that this component at 100 per cent load may amount to about three degrees centigrade.

Mr. Montsinger questions the data in Figure 6 of our paper, wherein we showed that the difference between the hot-spot temperature and the average winding temperature remain constant for loads ranging from 100 to 250 per cent. He states that in his tests the difference always increased with load. We have confirmed the results obtained in Figure 6 with tests on a number of other units and feel that Figure 6 is in reasonable agreement with the facts. A small proportion of the losses of the 600-kva transformer was supplied above the coils, but, due to the other tests made, we believe that this had little effect. Further, additional study of the results themselves confirmed this belief. There are some conditions, however, under which this effect will not hold. Two of the simplest of these are:

1. The case where the top coil is more heavily insulated than the coils in the stack.

2. If shielding or other construction details prevent as free coil ventilation in the top coils as in the stack.

However, Mr. Montsinger has made a more serious comment than his remarks regarding the accuracy of the test data. He does not agree that the results of our tests make possible recommending higher emergency overloads than those given in the ASA Guides for Operation of Transformers. We did use our test data in that we showed that many transformers had lower hot spots than had previously been believed or, at least, used in similar calculations. Also, we did show that the increased losses, due to increased copper resistance at higher temperatures, were counteracted by lower gradients and increased oil flow, due to changes in oil viscosity. The fact is that higher temperature limits than those considered in the present Proposed ASA Guide for Operation are given in the paper, and this presentation is in agreement with the papers and discussions of "Temperature Limits Set by Oil and Cellulose Insulation," by Dr. Charles F. Hill, AIEE TRANSACTIONS, volume 58, 1939, September section, page 484, and "Loading Transformers by Copper Temperature," by Mr. H. V. Putman and Mr. W. M. Dann, AIEE TRANSACTIONS, volume 58, October section, page 504.

It was not intended that the characteristics given in the paper and used in the calculation of Figure 7 of the paper necessarily applied to all transformers, nor that additional classifications might not be desirable. For this purpose, the authors are prepared to suggest a list of several types of transformers with their respective characteristics. The authors believe, however, that for any of these types, a 20-degree gradient suggested by Mr. Montsinger is high.

Mr. Hartzell's discussion is very interesting to us, since it shows that some operating engineers are also thinking that higher overloads can be carried for emergency service than given in the operating guides in the proposed ASA Standards. We agree that the values proposed in the paper are conservative. We hesitated to go to the extremes, although we may have fundamental data which would indicate possibilities, because

1. It would be a radical departure from past practice.

2. There are other factors involved, such as, contacts, bushings, tap changes, and so forth.

High-Speed Single-Pole Reclosing

Discussion and authors' closure of paper 42-24 by J. J. Trainor, J. E. Hobson, and H. N. Muller, Jr., presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 81–7.

S. B. Crary, L. F. Kennedy, C. A. Woodrow (General Electric Company, Schenectady, N. Y.): The detailed technical information at present available on the problem of high-speed reclosing seems to be well understood. There appears to be no outstanding differences in the results of the stability analyses presented in the Trainor, Hobson, and Muller paper and in our paper. Manufacturers can supply as standard equipment either single-phase or three-phase reclosing breakers. It is agreed there are fields of application for both.

However, there does appear to be a difference of opinion in the interpretation of the technical data. This difference is in the extent of the field of application for single-phase reclosing breakers as compared with three-phase reclosing breakers.

In most stability studies double line-to-ground faults and three-phase faults have been used as a basis for determining the stability of the system. It would appear, if all at possible, that faults involving more than one phase should continue to be the criterion for reliable power delivery. This philosophy of system design, if continued, would result in the emphasis being placed on high speed of clearing, with possible shorter deionization times, rather than on the development of single-phase slower speed reclosing systems for single line-to-ground faults only.

If this approach is followed, reclosing will become much more beneficial to the over-all performance of the system and give it a more important place in system protection, since it would generally allow for successful operation through even the most severe faults rather than only line-to-ground faults.

J. Trainor (Public Service Company of Indiana, Inc., Indianapolis, Ind.): The transmission system of Public Service Company of Indiana, Inc., as it now exists, contains 1,837 miles of 33-kv line, 796 miles of 66-kv line, and 409 miles of 138-kv line, with 120 additional miles of 138-kv line under construction and 45 additional miles planned for immediate construction. When the expansion of the 138-kv system was undertaken, careful consideration was given to the available means for improved operation. The importance of these lines as major links in the interconnection of the systems of Louisville Gas and Electric Company, Cincinnati Gas and Electric Company, American Gas and Electric Company, and Northern Indiana Public Service Company and Public Service Company of Indiana, Inc., makes it necessary that their operation be of the best. For example, our load in the Newcastle area is 25,000 kw, but the duty on the Lenore–Newcastle line has reached 65,000 kw during power transfers for Cincinnati Gas and Electric Company and Indianapolis Power and Light Company to the system of American Gas and Electric Company.

Because of existing insulation levels, ground-fault neutralizers could not be applied to the system with reasonable cost. We have watched the operation of three-pole reclosing on neighboring systems in Indiana with great interest and we recognize its value. However, we felt that definite improvement in operation would be obtained by the use of single-pole reclosing:

1. The disturbance to the rest of the system attending the dropping of load being carried by the line when a fault occurs will be decreased if the amount of load dropped is decreased.

2. While the amount of load to be carried by the line may not be near the stability limit when the line is constructed, major tie lines are often called upon to carry unforeseen amounts of power during emergencies—that is, loss of generating capacity or loss of other tie-line capacity—and any increase in the stability limit of the line may prove invaluable at such times.

3. It is our belief that single-pole reclosing offers all the advantages of three-pole reclosing, and will prove even better in most cases.

J. E. Hobson and **H. N. Muller, Jr.:** We are glad to have the comments of Crary, Kennedy, and Woodrow regarding the type of fault to be used as a basis for determining system stability. It has, of course, been the practice to predicate system design on the ability to endure a double line-to-ground fault. It might seem logical to revise this basis somewhat, as additional data regarding line performance become available, and to base system design on the probability of instability for all types of faults. This approach would include considering the likelihood of occurrence of the various types of faults, the probability of faults occurring, anticipated loads on the system as a function of time of the day and time of the year, and so forth. The basis of system design would then be an estimated allowable probability of outage, taking all of these factors into consideration. This philosophy of design can become more and more effective as additional data regarding line performance become available. Under the present system it hardly is reasonable to base the stability performance of a well-protected, high impulse-level line and the stability performance of an improperly shielded, low impulse-level line on double line-to-ground faults. The probability of occurrence of double line-to-ground faults per mile per year on the latter line may be even higher than the probability of occurrence of single line-to-ground faults per mile per year on the well-designed line, having well-co-ordinated line-ground and line-line insulation. The actual performance of the two lines, each carrying a block of power calculated as the permissible limit to "ride through" a double line-to-ground fault, would be far different. Since the proportion of all faults which will be of the double line-to-ground type is likely to be low on a well-designed line, the probability of any fault occurring is low, and the combined probability of a double line-to-ground fault occurring at the time of peak transmission may be very low, it is perhaps not illogical to base the stability limits of such a line on single line-to-ground faults.

Analysis of the Application of High-Speed Reclosing Breakers to Transmission Systems

Discussion and authors' closure of paper 42-31 by S. B. Crary, L. F. Kennedy, and C. A. Woodrow, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 339–48.

Eric T. B. Gross (College of the City of New York, N. Y.): When comparing high-speed reclosing with ground-fault neutralizers, it seems to me that all advantages of both systems should be considered. In this country, I believe, all earth-fault coils which have been put into service are being designed for a very short period of operation only, whereas hardly any coil in service anywhere abroad has been designed for less than two hours operation, but most of the coils are for continuous operation. In many cases it has been a distinct advantage that the operation could be continued for a few hours until the point of the fault had been located,[1] and the line-section with the fault switched off the system without any "shock" resulting from a ground fault. In such systems there are no automatic outages for any single ground fault so that the protection is extended from the self-clearing ground fault to all single-phase ground faults.

The temporary operation with two-phase wires at full line-to-line voltage above ground does not bring severe additional stress on the insulation of the system: in hardly any case will this occur for a period longer than 12 hours altogether in a year (the sum of the durations of all single-phase ground faults during 12 months). It seems to me that in this country the field of application of ground-fault neutralizers lies within systems of lower voltages, that is up to about 66,000 volts, and in such systems the cost of the neutralizer should be small in comparison with the cost of relays and apparatus for quick reclosing at each end of the line sections.

REFERENCE

1. SENSITIVE GROUND PROTECTION FOR TRANSMISSION LINES AND DISTRIBUTION FEEDERS, Eric T. B. Gross. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 968–71.

J. E. Hobson and **H. N. Muller, Jr.** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Power-system and application engineers appreciate the benefits to be obtained in increased transient power limits and reliability of service from the use of ground-fault neutralizers and high-speed reclosing, both gang- and single-pole operated. The scheme to be used in any situation will be determined after a careful evaluation of the engineering and economic factors involved. The curves presented by the authors, resulting from a-c network calculator studies, will be useful in helping to evaluate these engineering factors.

Since the most commonly encountered problem today is that of raising transient power limits and improving reliability of service on existing transmission systems, and since those existing systems 110 kv and above are, almost without exception, operated with a grounded neutral, the ground-fault neutralizer is usually uneconomical compared with other schemes. This is caused by the extensive system changes necessary to convert a grounded-neutral system to utilize a neutralizer.

Several of the generalized statements contained in the various sets of conclusions presented in this paper merit discussion. A few of the more important conclusions, relating particularly to the choice between three-pole or single-pole reclosing, will be considered.

The authors state that 50 per cent longer deionizing time may be required for satisfactory arc extinction on a 100-mile line if single-pole rather than three-pole reclosing is used. It is recognized that charging current from the sound phases will flow in the arc while the faulty phase is isolated, and it is also recognized that this may be an important or even limiting consideration when very long lines are involved. Existing data on time for arc deionization and on critical arc currents are so meager that the discussers question whether it is possible to state any figure such as the 50 per cent presented by the authors. It should also be remembered that charging current is not only a function of line length, but a function of the square of the voltage, so that any limitation imposed by line length has little meaning unless it is qualified to apply to a particular voltage class. It is not believed to be practical, in view of the absence of definite data, to state for what line lengths and voltage classes arc deionization becomes significantly longer when single-pole rather than three-pole reclosing is applied. We believe this factor is not likely to be important for the shorter lines usually encountered.

The authors state that single-pole reclosing appears to offer important advantage, only on single-circuit interconnecting lines, and when the generating capacity of one of the interconnected areas is not much greater than the load on the interconnecting line. We have performed calculations on a system where the receiver was assumed to be infinite, and where the sending end capacity was several times the load to be transmitted over the interconnecting line. Rather large gains in transient power limits for single-pole as compared to three-pole reclosing resulted for reclosing times of both 35 and 20 cycles. For 35-cycle reclosing this gain was 42 per cent, and for 20-cycle reclosing 22 per cent applies.

The authors also state that single-pole reclosing offers no advantage over gang operation for faults involving more than one phase. Our calculations show that for double line-to-ground faults the gain is smaller than for single-phase faults, but in particular cases this gain may still be quite significant.

Transient power limits obtainable with 20- and 35-cycle three-pole reclosing are compared with the limits obtainable with 35- and 60-cycle single-pole reclosing. Conclusions drawn by the authors from such a comparison are somewhat exaggerated in favor of the three-pole operation, even if the 50 per cent longer deionizing time given in the paper is assumed to be necessary for satisfactory single-pole operation, since 35-cycle operation permits over twice the de-energized time that 20-cycle reclosing permits. A similar comparison exists for 60- and 35-cycle reclosing speeds. Even then, a conclusion drawn from this comparison states that a gain is realized by the use of 35-cycle single-pole as compared to 20-cycle gang reclosing.

We suggest that the more rigorous method of symmetrical components could be used to advantage in calculating the arc current in the faulted phase during the de-energized period, rather than the approximate method used by the authors. The quantity V_a' in the equations of appendix III may differ considerably from 0.5 V_b under the unbalanced condition with one conductor isolated.

J. J. Trainor (Public Service Company of Indiana, Inc., Indianapolis, Ind.): These authors seem to prefer the use of ground-fault neutralizers to that of single-pole reclosing.

Probably no one in the country is better aware of the operating advantages and the improvement in line outages and service interruptions to be gained through the use of ground-fault neutralizers than we. In the United States, the fifth installation of ground-fault neutralizers and the sixth neutralizer, was placed in service by Public Service Company of Indiana, at New Castle, Indiana, on December 6, 1936, on a 33-kv delta transmission system having about 160 miles of wood-pole line without static wires. After a few months' observation of the operation of this neutralizer, its value was appreciated, and four additional neutralizers were ordered from the manufacturer. The seventh installation in this country, involving these four units, was completed on August 15, 1937, for the protection of approximately 1,300 miles of 33-kv line without static wires on a delta system. At this time, we had 45 per cent, or 5 of the 11 neutralizers in service in the country, protecting 54 per cent, or 1,460 of the 2,700 miles of line so protected.

It may be of interest to note that we operate some 1,200 miles of 33-kv delta line in parallel with three ground-fault neutralizers on the parallel system. This operation was not at all practical before the installation of the neutralizers, and we were obliged to separate this section of the system into three isolated parts. It may be of further interest that we note no appreciable difference in the ratio of line outages to total system faults on the system having three neutralizers in parallel to the same ratio on the two isolated sections protected by individual units, the ratio being 60 per cent to 65 per cent in either case. Up to December 31, 1941, the neutralizers had successfully cleared 1,687 of a total of 2,634 system faults, or 64 per cent.

However, the present discussion is concerned with the operation of 138-kv transmission systems, most of which are designed and insulated for grounded-neutral operation. The application of ground-fault neutralizers on such a system requires that all line, insulation, and arrester voltages be increased to the value required for delta operation on the entire system, or that these be increased to the required value on that section of the system to be protected by the neutralizer, and an insulating transformer installed between that section and the remainder of the system. In view of the practically complete interconnection of the entire 138-kv systems in the eastern half of the country, involving thousands of miles of line and numerous substations, the isolation of particular systems by means of insulating transformers would be most expensive. This difficulty would be further complicated by the number of points at which interconnections occur. In our own system, we would require five insulating transformers for complete isolation, the locations being Lafayette, Kokomo, New Castle, Cincinnati, and Louisville. Such transformers would limit the useful capacities of the lines to the capacities of the transformer banks.

S. B. Crary, L. F. Kennedy, and **C. A. Woodrow:** The discussers indicate some differences of opinion as to the interpretation of the network-analyzer stability results, although not in the results themselves. This is not surprising, as the application of these results depends upon all the factors including the economic ones which affect the individual system or situation. Our objective was to show the technical factors which enter into the selection of means for improving reliability and the necessity of basing a selection upon the system characteristics.

Messrs. Hobson and Muller believe that a 50 per cent longer deionization time for satisfactory arc extinction on a 100-mile line, if single-phase rather than three-phase reclosing is used, is overly conservative or pessimistic. This assumption was made to illustrate the use of the curves in obtaining a comparison between single-phase and three-phase reclosing. The curves are such that any assumed de-energization time may be used. However, the general conclusions will be unchanged whether the single-phase reclosing time is or is not appreciably increased over three-phase reclosing. For the line-to-ground fault case, the rate of change in power limit with de-energization time is not very great. Messrs. Hobson and Muller point out that they have calculated rather large gains in power limits for single-phase as compared with three-phase reclosing, in one case 22 per cent gain in limit for 20-cycle reclosing. Although they do not specifically say so, this is undoubtedly for a line-to-ground fault. For the conditions they have outlined of an infinite receiving end and a sending-end capacity several times the transmitted load, it may be questionable whether this additional increase in transient limit for a single-line-to-ground fault is necessary. We have found that for systems of this kind the line-to-ground fault power limit with single-phase reclosing is usually appreciably higher than the load required to be transmitted over the circuit. This, we believe, is an important consideration when determining the advisability of using single-phase reclosing rather than three-phase reclosing. For systems of this type, as pointed out in our paper, three-phase reclosing can adequately provide stability for even the more severe three-phase faults with 20-cycle reclosing for the required line loadings. For short lines, where the thermal limitations of the circuit set the maximum power which can be carried, it may not be economical to use means which increase the stability limit for line-to-ground faults above the thermal load limit.

For double-line-to-ground faults we cannot agree with Messrs. Hobson and Muller that the gain may still be quite significant over three-phase reclosing. In the first place the gain is small even for the same reclosing time. Secondly, on many systems the number of three-phase faults may be as great or greater than the number of double-line-to-ground faults. Thus the advantages of single-phase reclosing for other than line-to-ground faults are very questionable.

In our paper results are shown for wide-range reclosing times. In one section of the paper we interpreted these results in terms of present-day standard breakers. Since the present application of single-phase reclosing breakers has been for longer reclosing times than three-phase reclosing breakers, it was natural for us to compare 20- and 35-cycle three-phase reclosing breakers with 35- and 60-cycle single-phase reclosing breakers, respectively. However, any other sort of desired comparison can readily be made from the data included in our paper. We do not believe that reasonable variations in practical comparisons will give results which are inconsistent with our general conclusions.

Messrs. Hobson and Muller suggest a more rigorous method be used for calculating the arc current in the faulted phase during the de-energization period rather than the approximate method used in appendix III of the paper. In using the network analyzer we represented the system as three separate phases with a neutral return circuit representing the ground and ground-wire return. With this representation we were able to impose on the circuit the simultaneous dissymmetries which occur, due to clearing line-to-ground or double-line-to-ground faults with single-phase switching. On the network analyzer the currents in the arc and the fundamental frequency recovery voltage after the fault is cleared were easily obtained during the angular swing when the faulted conductor is de-energized, while the method of symmetrical components becomes quite involved when the number of simultaneous dissymmetries exceeds two. These results indicated that for the angular displacement at which reclosure must take place in order to maintain stability, the magnitude of the arc current and the steady-state recovery voltage was not reduced very much from that given in appendix III. Because of this confirmation, it was felt that the approximate method gave a reasonably correct quantitative value for the arc current and recovery voltage which should be allowed for during single-phase de-energization. Furthermore, the current and voltage at the arc would conform most nearly to the approximate method under those conditions of light system loading and would be (except for effect of distributed constants) almost entirely correct at no load.

We regret that the limited reference to ground-fault neutralizers has lead Messrs. Hobson, Muller and Trainor to emphasize the limitations of this device under some conditions where high-speed reclosing appears to be the sound solution. We had no intention of attempting to offer the ground-fault neutralizer in place of reclosing. It was felt, however, that it should be mentioned as it provides an easy means of minimizing the effects of single-phase-to-ground faults where applicable.

Mr. Gross's discussion calls attention to differences between foreign and domestic practice in the operation of ground-fault neutralizers. The question of length of time to operate a ground-fault-neutralizer system with a permanent ground fault depends on many factors. In general, operators in this country prefer to switch permanent faults off quickly. This is the case even when neutralizer coils have been provided with a continuous rating. The type of system influences this considerably. If the system is of relatively low voltage 33 to 66 kv, for example, and essentially radial in character covering a large area, it becomes good practice to switch permanent faults off quickly in order to avoid simultaneous faults and an increase in the amount of load interruption. Other advantages also result, such as reduction in telephone interference, patrol time, and number of incorrect relay operations due to simultaneous faults.

Performance of Ground-Relayed Distribution Circuits During Faults to Ground

Discussion and authors' closure of paper 42-8 by C. L. Gilkeson, P. A. Jeanne, and J. C. Davenport, Jr., presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 40–8.

William Shuler (Dayton Power and Light Company, Dayton, Ohio): I wish to give an operating man's impression of the value of instantaneous ground relaying on distribution circuits during faults to ground. We started originally with ground relays connected on two 12-kv rural circuits. These relays operated in from one to two cycles, and, after clearing the fault, the breaker was immediately reclosed. The average time from the initiation of the fault to the reclosing of the breaker was approximately 35 cycles, and an operation for the second opening, should the fault persist, was controlled by time-induction relays which were set to permit main and branch-line sectionalizing fuses to blow. We were so well satisfied with the success of this scheme that ground relays either have been or are being applied to all of our four-wire feeders, including our heavily loaded power circuits in urban territory.

Some tests were made on one feeder supplying a customer who has a large load consisting of squirrel-cage, slip-ring, and synchronous motors, as well as rotary converters. A fault was placed on this circuit which was cleared, and the breaker reclosed without losing any motor load except two motors whose service depended upon contactors energized from the a-c source. The 250 volt d-c rotary converter, which was loaded only to 25 per cent, went through the interruption without flashing over or dropping out of step.

As I see it, the use of ground relays and instantaneous reclosures on overhead circuits has two advantages:

1. It gives a type of service almost equal to duplicate service.
2. It very materially reduces the damage to overhead conductors because of fault currents.

In applying ground relays to circuits having main or branch-line sectionalizing fuses, it is necessary that maximum short currents available be known, and that a fuse be selected having a time current characteristic which will permit the fuse to go through one operation without damage before the relay and circuit breakers clear the circuit.

In some cases it has been necessary for us to install reactors at the substation to hold the short-circuit current down to a value which will protect the fuses. On those feeders where this has been worked out carefully, no trouble has been experienced, and, although our rural feeders are very heavily fused, we have relatively few main or branch-line sectionalizing operations.

In the paper mention is made of the fact that 40 amperes is about as low a fuse as can be used successfully. It is my opinion that fuses having a lower rating may be used on long circuits since it is only a question of the fuse being able to carry without damage the short-circuit current available for longer than seven cycles.

I also want to mention the fact that we are now occupying poles jointly with The Ohio Bell Telephone Company, in territory near our urban district but removed from our four-kv system where we need only one phase wire and a neutral of a 12-kv feeder and can fuse the branch lead at a value which is satisfactory to the telephone company engineers. An engineering study is made of each case, but as a result of several years' co-operation we now have many poles jointly used.

I want to express my appreciation for the work done by the project committee of the Joint Subcommittee on Development and Research that collected the data which formed the basis for this paper. Our company is of the opinion that this work was of a very considerable value to us.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The widespread use of instantaneous reclosing relays has greatly enhanced the value of any operating scheme which permits substituting additional short-time interruptions in order to reduce the number of sustained interruptions. The comprehensive oscillograph information of the four companies' studies is excellent confirmation of the good performance secured by a large number of operating companies with instantaneous reclosing and ground relaying.

Several years' experience on the system of the former Tennessee Electric Power Company convinced the writer that even more sensitive ground settings than the author's suggested limit of 25 amperes may be used without danger of "tree grounds" causing unnecessary operations. Many three-phase, three-wire, 11-kv grounded neutral circuits on that system were operated with ground relay settings as low as 10 amperes primary.

There is some advantage in following the practice of one of the companies studied, namely, that of permitting the high-speed relays to be in operation for the instantaneous reclosure, as well as for the original disturbance. Objects such as tree limbs which are blown onto the line will sometimes fail to fall away from the line in the brief interval of instantaneous reclosing, and the second operation of the fast ground relay prevents an unnecessary sustained interruption to the affected branch at the expense of a few seconds' interruption to the entire feeder.

Fortunately, the telephone cable or conductor of a joint use installation provides a low enough resistance path to ground so that reliable operation is obtained with ground relays set on the order of full load current. It is much more difficult to insure that a fallen conductor in contact with the ground will be cleared properly, since contact resistance may limit the fault current to a value materially less than the full load current. In the case of three-wire circuits, or even four-wire circuits with neutral grounded at the supply station only, ground relays may be set independent of load current, and standard devices provide as sensitive settings as practicable. However, devices and methods are still lacking for providing sensitive ground relaying on the more common three-phase, four-wire circuits, especially at the lower distribution voltages.

G. Fred Lincks (General Electric Company, Pittsfield, Mass.): Messrs. Gilkeson, Jeanne, and Davenport's presentation of operating data indicates the advantages of a system of relaying that apparently is being adopted by an increasing number of utility companies. Their data support the conclusions as to the improvement in service continuity obtainable with such relaying as presented in my paper, "Relative Values of Different Types of Overcurrent Protection for Distribution Circuits." Table I of this discussion, giving additional data to that in the curves of my paper, shows the relative improvement obtainable with single-element fuse cutouts employed at each of the branches or at a number of the sectionalizing points connected in series, when nonreclosing relays, reclosing relays, or reclosing relays with the instantaneous ground relaying facilities are used. The authors only refer to branch-line fusing. Table I shows that relaying—so the substation breaker opens once ahead of all sectionalizing or branch protective devices and then provides time delay so these devices will clear ahead of the second opening of the breaker—generally provides greater benefits in the improvement in service continuity with line sectionalizing than with branch protection. Such relaying permits securing greater reductions in consumer minutes outage with less costly branch and line-sectionalizing equipment than is required with nonreclosing or reclosing relays and breakers. For example, single-element sectionalizing fuses with the instantaneous ground relaying are more effective than an equal number of three-element reclosing cutouts with just a reclosing relay.

The authors state that "instantaneous ground relays would not be expected to save branch-line fuses below 30- or 40-ampere rating." It is assumed that this limitation applies to the system for which the data are reported. In setting up such a relay and line protective scheme, the minimum fuse rating would be determined by comparing the total clearing time for the relay and breaker with the melting time-current characteristic curves for the fuses. A 25 per cent factor should be applied for variables (see Figure 14, "Trends in Distribution Overcurrent Protection" by G. F. Lincks and P. E. Benner, AIEE TRANSACTIONS, volume 56, 1937, January section, pages 138–52). The minimum fuse rating which will provide the desired selectivity of operation will depend on the relay setting and breaker opening time and the current available at the point on the system where the line protective device is located. In studies on actual circuits

Table I. Comparison of the Effectiveness of Substation Protective Equipment

With Single-Element Cutouts and 75 Per Cent Temporary Faults

No. of Branches or Sectionalizing Points	Substation Protection: Nonreclosing Breaker	Substation Protection: Automatic Reclosing Breaker	Substation Protection: Reclosing Breaker Set for 1st Opening Ahead of Line Fuses	Maximum Protection Obtainable (Permanent Faults Only Cause Outage)
	Per Cent Consumer Minutes Outage*			
Protecting 5-Mile Branches				
0	115.7	101.0	101.0	100.0
1	85.0	75.0	73.9	72.2
2	65.5	57.8	56.1	54.8
3	51.8	46.0	44.2	42.8
4	42.0	37.2	35.6	34.5
5	34.8	31.0	29.4	28.4
6	29.2	26.1	24.7	23.8
Protecting at Sectionalizing Points				
0	115.7	101.0	101.0	100.0
1	100	92.6	74.3	64.0
2	92	87.0	64.5	53.2
3	87.5	83.4	60.0	48.0
4	84.2	81.3	57.0	44.0
5	81.7	79.5	54.8	42.3
6	80.8	78.5	53.5	40.9

$$\text{* Per cent} = 100 \times \frac{\text{Consumer minutes outage caused by permanent faults plus temporary faults for any specific system setup}}{\text{Consumer minutes outage caused by permanent faults alone with no branch protection or line sectionalizing}}$$

it has been found that with instantaneous ground relaying, proper selectivity of operation is possible with fuse ratings as low as 10 amperes where such fuses are located well out on the line where the available short-circuit currents are low.

The authors' report on the high quality of the performance of fuses is indicative of the improvement that has been made in this product during the past few years. It shows that modern fuse links are meeting the requisites of greater dependability and accuracy demanded by such co-ordination practices.

C. L. Gilkeson, P. A. Jeanne, and **J. C. Davenport, Jr.**: The authors wish to thank Messrs. Shuler, Brownlee, and Lincks for their interesting and instructive discussions of this paper. In reply to the question regarding the statement in the paper that instantaneous ground relays would not be expected to save branch-line fuses below 30- or 40-ampere rating, the authors were referring to relatively high kilovolt-ampere capacity feeders, such as those on which most of the observations were made. Where the fault current is limited to relatively small values, either due to long feeders or neutral impedance, smaller fuses could be protected, as Mr. Shuler stated.

In suggesting minimum ground-relay settings of 25 to 35 amperes, the authors wished to be conservative, particularly with respect to four-wire multigrounded neutral feeders. As Mr. Brownlee states, lower settings may be feasible, and have been used on three-wire unigrounded neutral systems.

The following errors appeared in this paper as published in the AIEE TRANSACTIONS (volume 61, 1942, January section, pages 40–8):

1. Table II. The subheading "Instantaneous" under "Ground Relays" applies only to the column just preceding "Reclosure Practice."
2. Table III. In the footnote ## "cleared" should read "clear."
3. Table B. In the title "Local" should read "Load."

Relative Value of Different Types of Overcurrent Protection for Distribution Circuits

Discussion and author's closure of paper 42-2 by G. F. Lincks, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 19–26.

R. O. Loomis (Georgia Power Company, Atlanta, Ga.): Line-sectionalizing devices provide an economical means of reducing interruptions to electric service, which means improving the quality of the service. As in practically all engineering problems, the cost is the important element. Since investments in distribution lines are an important part of total system investments, opportunities for reducing these investments cannot be overlooked. Mr. Lincks has compared in this paper the relative value of different types of devices, enabling selection of the method providing the greatest improvement at the lowest cost. Mr. Lincks has made a definite contribution by providing a method of comparison. This paper is very pertinent at the present time when restrictions in material will prevent more expensive methods of improving service.

In applying sectionalizing devices, it should be remembered that proper co-ordination is essential. I suppose that one of the assumptions employed was that perfect co-ordination was obtained, and that no fuses broke mechanically or blew because of poor mechanical condition of cutouts. Some forms of cutouts tend to overstress the links mechanically upon closing. Cutouts in poor condition may overheat. Mechanical failure of reclosing fuses to reclose occurs. It will be necessary to use only one make and type of link on a given line. This usually means the use of only one make and type of link by the entire company, or at least by one operating district. Attention to such details is necessary to approach perfect operation. I would like to know what the effect on the results would be if an assumption had been made that, say one fuse link out of ten blew incorrectly, or unnecessarily.

Assumption 6 states that a trouble crew is available to start instantly. In rural areas where much of the sectionalizing is employed, this is seldom true. The additional time required apparently increases the benefits of sectionalizing.

In assumption 7, permanent faults are repaired very rapidly. Sometimes such faults require a crew larger than the trouble crew, and sometimes they are difficult to find, so I am inclined to think 30 minutes on the average is low. Increasing this time apparently increases the benefits of sectionalizing. Sectionalizing helps in locating the faults difficult to find, which is an added advantage.

Assumption 10 is often not true in rural areas. On the other hand, in urban areas where short-circuit currents are larger, and relay current and time settings are higher, because of larger loads, and to co-ordinate with larger transformer fuses, branch fusing may prevent burning down lines, since the branch fuse may blow in a time short enough to prevent damaging the conductors, where the substation breaker will not open quickly enough to prevent burning of conductors. This advantage is also obtained by using pole-mounted oil circuit reclosers, but they are not so adaptable to urban distribution as sectionalizing fuses. Although mechanical breakdown of conductors by automobiles, trees, and so forth, occur, I have not found a case where conductors burned down when protected by oil circuit reclosers.

The curves presented deserve careful study. They show the effects of a high percentage of temporary faults, particularly on line sectionalizing. I am surprised that the percentage of temporary faults does not have more effect in branch fusing as shown in Figure 5 of the paper. The conclusion that "Single-element fusing of branches provides approximately 85 to 95 per cent of the total improvement obtainable" is rather startling to me. I would like to know what effect the percentage of temporary faults has on branch fusing when all the customers are on the branches: for example, when all branches at the end of a trunk feeder are fused. Other conclusions appear reasonable and justified.

C. R. Craig (General Electric Company, Pittsfield, Mass.): Because of the fact that conclusions drawn from a mathematical study of operating conditions must necessarily be tempered by experience and inherent characteristics of individual systems, it should be borne in mind that the benefits of the different types of protection presented in Mr. Lincks' paper for a system as a whole may just as well be applied to only a portion of a particular system. If this portion of the system were subject to an especially high percentage of temporary faults, as compared to the system as a whole, an efficient combination of protection on the troublesome branches or small section of feeder would materially reduce the percentage of temporary faults on the entire system, thereby lowering the over-all outage time.

In paragraph 5, under the heading "Of What Value Is Branch Protection," the author calls to attention one of the most important conclusions. This shows that single-element cutouts furnish the major percentage of the total improvement attainable by branch protection. From the standpoint of initial cost, this means that the single-element cutouts give the most protection for the money invested. For example, let us assume some comparative costs of protective equipment, and then determine the comparative benefits of the combinations of protective equipment.

While actual costs may vary a great deal, for the sake of illustration, the following somewhat arbitrary values are assumed for initial installed costs:

One-element cutout	$ 20 each
Two-element cutout	30 each
Three-element cutout	70 each
Line automatic recloser	120 each
Nonreclosing substation Breaker and relays	1,000 each
Automatic reclosing substation Breaker and relays	1,350 each

The division of the total cost of branch protective equipment by the cost of substation protective equipment alone will give the per cent increase in initial cost of combined substation and branch protection over that for no branch protection. This result can be compared with the per cent improvement in system protection as shown in the paper for the corresponding system setup (see Figures 5 and 8 of the paper). A summary such as Table I of this discussion, constructed from the above assumptions and data calculated for the paper, serves to illustrate the comparison between attainable improvement in protection and the cost of the protective equipment.

Even though the above assumed comparative costs may not be exact for cutouts or breakers, it is evident from Table I, for example, that with a nonreclosing substation breaker, single-element cutouts on branches furnish 52.7 per cent improvement in protection with only 6 per cent increase in total installed costs over no branch protection. An increase in cost of 15 per cent (21–6 per cent) for three-element cutouts over the cost of single-element cutouts is necessary to obtain only 1.3 per cent (54–52.7 per cent) increase in protection furnished by the three-element in place of single-element cutouts. Similar comparisons may be made with the

Table I

Substation Protection	Three 5-Mile Branches† Branch Protection	For 75 Per Cent Temporary Faults			
		Consumer-Minutes Outage in Per Cent of "Yardstick"	Initial Cost	Per Cent* Improvement in Protection	Per Cent Increase in Cost
Nonreclosing breaker ($1,000)	None	110	$1,000		**
	3 One-element cutout ($20 each)	52	1,060	52.7	6
	3 Two-element cutout ($30 each)	51	1,090	53.6	9
	3 Three-element cutout ($70 each)	50.6	1,210	54.0	21
	3 Automatic recloser ($120 each)	50	1,360	54.5	36
Automatic reclosing breaker ($1,350)	None	100	1,350		#
	3 One-element cutout ($20 each)	46	1,410	54	4.5
	3 Two-element cutout ($30 each)	45	1,440	55	6.7
	3 Three-element cutout ($70 each)	44.7	1,560	55.3	15.5
	3 Automatic recloser ($120 each)	44	1,710	56	27
Permanent faults only		43		##67	

* Per cent improvement in protection = attainable decrease in outages, expressed as a per cent of the per cent consumer-minutes outages attainable with no branch protection. Example—$\frac{110-52}{110}\times 100=52.7$ per cent.

**100 per cent base cost = $1,000 for nonreclosing breaker, relays, etc. (Substation protection only.)

#100 per cent base cost = $1,350 for automatic reclosing breaker, relays, etc. (Substation protection only.)

##67 per cent in this case represents the maximum per cent of decrease in outages attainable (all temperature faults cleared immediately).

†Assume 7,500-volt single-phase, line-to-ground neutral, requiring only one protective unit per branch.

other types of equipment and with the automatic reclosing breaker at the substation as shown in the table. This example illustrates how actual costs may be applied in the author's mathematical study to be used as an aid in actual system planning.

To the loss of revenue and initial costs, there must be added the cost of maintaining the protective equipment on a system. It would be possible and advantageous to determine comparative maintenance costs for various combinations of protective equipment from a similar mathematical study with assumptions and conditions parallel to those in the paper under discussion. Calculations of actual outage time alone would serve as a basis for figuring labor costs for restoring service after outages with the different combinations of protective equipment. Auto and truck mileage would form a portion of the maintenance cost, but from the assumptions in this paper they would be uniform and proportional to the number of miles of line, because the number of outages and consumers per mile were assumed uniform. While it might be difficult to introduce actual costs into a maintenance study, because of the diversity of setups of distribution systems, comparative percentages would show the relations between the values of different types of overcurrent protection and the labor time necessary to maintain the protection.

Bruce O. Watkins (nonmember; Rural Electrification Administration, St. Louis, Mo.): Mr. Lincks is to be congratulated for this contribution to the solution of distribution-line-sectionalizing problems.

The so called "area-electrification" program such as practiced by the Rural Electrification Administration opens up an entirely new field in distribution engineering. So far distribution engineers have only scratched the surface on this subject.

Rural distribution systems financed by the Rural Electrification Administration vary widely in characteristics, but for purposes of discussion, a "representative" system as such usually involves between 300 and 400 miles of line, served by a single substation. Most systems are sectionalized by means of fuses. The substation protection consists of a three-shot reclosing fuse cutout, with the line sectionalized by two or three-shot cutouts. Branch laterals are usually controlled by a single-shot cutout. Operating practices on each system vary to suit local conditions, but for the purpose of this discussion the following practices are considered:

1. Substation is unattended.
2. Very little or no communication facilities are available.
3. Roads are usually poor, particularly during wet weather.
4. Line-crew personnel live a great distance apart or are usually out connecting new consumers or maintaining the lines.
5. Ends of lines may be 60 miles or more from the central office.

It can be seen that the above conditions are vastly different from those encountered in city or urban distribution.

Systems fused in the above manner have not given the results that have come to be expected for good service, particularly in lightning areas. Due to inadequate communication facilities, outages are not always reported promptly, and when they are reported, the problem of assembling a crew delays the restoration of service. The actual patrol of the lines to locate the trouble, and the travel over poor roads further increase the outage time.

To solve the communication problems, REA has been experimenting with two devices:

(*a*). A carrier-current outage recorder which immediately notifies the office of the location of an open sectionalizing device.

(*b*). A carrier communication system with portable transmitter–receiver and coupling capacitor in the trucks and with fixed equipment in the office.

Single-pole automatic reclosing circuit breakers, described by Mr. Lincks as "resetting reclosers," were installed on some of the earlier Rural Electrification Administration systems in conjunction with gap-protected transformers. Though many of these reclosers failed (failures largely due to improper insulation co-ordination), the results on the whole were so encouraging that in 1939 a program for installing an unprecedented number of such reclosers on systems located along the eastern seaboard was begun.

Table I indicates the results of a survey in one of the midwestern states where this type of recloser is being used. Gap-type transformers are in use on most of these systems. Since the recloser design was changed during this period, the two types of reclosers in use are differentiated by the terms 1 and 2, number 2 being the later design. All reclosers are of the same make.

On the eastern seaboard projects the reclosers are installed both on feeders and branches so that each one controls about 20 miles of line. No fused cutouts are installed on the load side of the recloser. In some cases reclosing fused cutouts are used for feeder sectionalizing and branch protection near the substation. Most of these systems have conventional arrester-protected transformers.

So far there has not been sufficient data accumulated to draw any final conclusions on the performance of these reclosing circuit breakers installed along the eastern seaboard, but the available information indicates much better operating results, both in shortening the length of interruption and decreasing maintenance costs, than with the previously fused systems. Quantitative results are being accumulated, but have not yet been tabulated; indications are that the per cent of lockouts to total operations is less than 10 per cent, and in some cases less than 5 per cent, during the lightning season. Only one complete recloser failure has occurred to our knowledge out of about 1,500 installed. Unfortunately, quantitative results for the previous fused cutout operation were not obtained, and hence no numerical comparison can be made. However, operating reports are not complete, but they do indicate that service has been improved considerably with the reclosing breakers and would seem to bear out the conclusion reached by Mr. Lincks that a very substantial improvement over fused cutouts is possible by using the recloser for sectionalizing, where there is a high percentage of temporary faults.

I agree with Mr. Lincks that continuity of service is always an operating engineer's major object, but there is also another angle to economical system operation. How much can we spend to eliminate fuse replacements? Estimates from Rural Electrification Administration system managers indicate that a few trips to refuse a distant cutout will cost much more than investment and maintenance costs on the recloser.

Since most faults are temporary, the recloser eliminates the majority of the previously necessary refusing trips.

There are still disadvantages to the presently available reclosing single-pole breaker:

1. As Mr. Lincks points out, it is not always practicable to provide automatic selectivity between the smaller size reclosers and the transformer fuse. This is particularly true for internally fused transformers.

2. There is an insufficient number of recloser sizes which will co-ordinate for proper sectionalizing on lengthy lines.

Both of these difficulties can be overcome, to some extent, by the introduction of inverse time delay in the recloser opening. New design reclosers have appeared on the market with such inverse time delay, but the experience with them is insufficient as yet to form any conclusion. Rural Electrification Administration is instituting further development of this inverse-time-delay feature in the recloser opening.

Table II. Report of Single-Pole-Reclosing Circuit-Breaker Operation on 20 Rural Systems in a Midwestern State to November 1, 1940

Most Systems Are Equipped With Gap-Protected Transformers

Total number of circuit reclosers reported	417
Total reported circuit recloser months of operation	5,098
Total number of operations reported	20,973
Total number lockouts reported	271
Per cent of lockouts to operations	1.29
Total operations where operations and number of reclosers were both reported	20,973*
Total number of reclosers as of above	301*
Average number of operations per breaker where both were reported	69.6
Average number of operations reported per recloser per month	4.11
Total reported number of recloser failures due to:	
Lightning	24
Worn parts	2
Oil	
Other	21
Total reported recloser failures, including those not segregated	59
Percentage of total recloser failures to number installed where both were reported	16.1
Percentage of total recloser failures per 12 months, where number of failures and months of operation were both reported	13.5
(Excluding system 18 where installation date was not given)	
Total reported failures:	
Design 1 reclosers	56†
Design 2 reclosers	3
Percentage of total design 1 recloser failure per 12 months, where number of failures and months of operation were both reported	21.8
Percentage of total design 2 recloser failures per 12 months, where number of failures and months of operation were both reported	2.04

*Number of reclosers and operations not always simultaneously reported.

†Systems 10 and 14 indicate failures but do not give the number. Other information on hand for system 10 indicates not more than two failures occurred, both due to recloser misapplication in interrupting rating.

Mr. Lincks states that overlapping the substation reclosing protection with single-element fusing at the branches or sectionalizing points approaches very closely to providing the minimum consumer minutes outage attainable.

Such a scheme is used on Rural Electrification Administration systems in the Tennessee Valley Authority area and works very satisfactorily. However, I do not feel that it is advisable on an extensive system.

1. Outages, even if momentary, are objectionable if over a large system.

2. It is difficult to reach all the way out on the system with the substation breaker.

3. The cost of the reclosing substation breaker would cover a great many single-pole reclosers.

In my opinion, definite advances can be made in rural distribution sectionalizing by the development of a small reclosing breaker similar to the present recloser, but with practically instantaneous first opening and inverse time delay on succeeding openings. The first reclosure should be as rapid as possible. Such a device should be inexpensive and should reset automatically. In order to reduce outage time on temporary line faults, the use of this breaker is advisable on most rural distribution feeders and long branch lines. By means of the instantaneous first opening and inverse-time-delay second opening, all short branch lines then could be protected with single-shot fuses, thus achieving the best over-all results. This idea seems to agree fairly well with the conclusions reached by Mr. Lincks concerning a reclosing breaker-fused branch combination, although Mr. Lincks discusses a different type of breaker. Also, I believe that to reduce outage time, the device should be single-pole, and, in order to prevent three-phase motor burnout with this device, effective and economical three-phase motor protection should be simultaneously developed.

Perhaps some new type of apparatus can be developed which will accomplish these results. At any rate continued research on the problem should advance the rural circuit sectionalizing art.

L. G. Smith (Consolidated Gas, Electric Light and Power Company, Baltimore, Md.): The author has prepared an excellent paper. In the field of distribution engineering there is a need for more papers of this type. In such studies the analytical approach based upon operating data should lead to conclusions that indicate:

1. Where to spend capital and maintenance dollars to obtain the best operating results by directly comparing operating benefits with the cost.

2. Where capital and maintenance dollars should not be spent.

3. What can be done to obtain the maximum improvement in continuity to service.

This paper should inspire further engineering studies of this type. However, additional operating data are necessary on which to build premises for further analytical studies.

The author refers to temporary faults varying from 15 to 85 per cent of the total. It is believed that in the usual distribution system the percentage is certainly above 50 and probably more of the order of 70.

Table III. Per Cent Consumer-Minutes Outage. Relative Effect of Various Methods of Branch Fusing

Three 10-Mile Branches

Station Protection	Fusing of Branches	75 Per Cent Temporary Faults (Per Cent)	85 Per Cent Temporary Faults (Per Cent)
Nonreclosing breakers	none	107	113
	1 element	33	40
	2 element	32	38
	3 element	31	37
Reclosing breakers	none	101	103
	1 element	30	35
	2 element	29	33
	3 element	28	31
Reclosing breakers set for instantaneous trip on first opening. Branch protective device delayed beyond first opening of breaker	none	101	105
	1 element	28	31
	2 element	27	30
	3 element	27	29

With the help of summary tables in a preliminary copy obtained from the author, Table III was prepared. If I have correctly interpreted the data, and if the data on one curve are comparable with those of another, I question the validity of two apparent conclusions derived from the author's data:

1. The data indicate less than a 10 per cent reduction in customer minutes outage by changing from a nonreclosing to a reclosing station oil circuit breaker as compared with about a 60 per cent improvement for branch fusing. The operating data of the company with which I have been associated show that 70 per cent of feeder troubles are restored without a lockout by reclosures of the station breakers.

2. In view of experience with reclosing station breakers, it is believed that actual operating experience would show a greater advantage in two or three element branch fuses.

G. F. Lincks: The author greatly appreciates the contributions made by those discussing the paper. There have been a number of points brought out in these discussions which should enhance the paper's value for those interested in the improvement of service continuity on distribution systems.

As Mr. R. O. Loomis points out, the study presented in the paper is based on securing perfect co-ordination. It has been experienced by a number of utility companies which, in addition to standardizing on only one type and make of fuse link for a given line, eliminated the possible causes of mechanical breakage of the fuse links, and poor mechanical and thermal conditions in the cutouts as well. Without doubt, Mr. Loomis had in mind reviewing the possible benefits to be derived from such elimination of defective devices, in raising the question as to the effect on the results "if, say, one fuse link out of ten blew incorrectly or unnecessarily." "Incorrectly" might imply improper sequence of operation without any increase in the actual number of outages, whereas "unnecessarily" would tend to indicate an increase in the number of outages. In either case, the effect on the results would vary with the location of the cutout which was causing the improper operation. In analyzing this point, it was assumed that the causes of such difficulty would be localized to one or two specific cutouts and would not be spread evenly over the whole line.

In case one out of ten devices operate "incorrectly," it was assumed that the circuit was opened at the next point closer to the substation than at the point where incorrect operation occurred. With line sectionalizing, this would tend to decrease by about one-tenth the improvement provided by the defective single-element or reclosing sectionalizing device. With single-element or reclosing branch protection, the station breaker would open, taking out the whole line instead of isolating the fault to the individual branch. Thus, the improvement over the "Yardstick" as provided by the branch protection employed under the specific system setup would be reduced by one tenth. The effect of defective equipment which causes incorrect operation is considerably greater at branches than at sectionalizing points.

In the case of devices operating "unnecessarily," and thus causing an additional number of outages to the one per mile used in the calculation, in the ratio of 1 to 10, it would be necessary to add from two to six units to the percentages shown on the curves for line sectionalizing with protective devices connected in series. The two should be added if the troublesome sectionalizing device is located close to the substation, and the six added if the troublesome device is located at the last sectionalizing point on the line. It varies about proportionately in between. With branch protection the effect is negligible, amounting to the addition of one unit to the percentages shown on the curves. With either branch fusing or line sectionalizing, the improvement provided by a defective reclosing device would be reduced by one tenth of the difference between the percentage shown on the curves for the actual device and for a device having one less reclosure.

Mr. Loomis is interested in the effect of the percentage of temporary faults on branch fusing, when all the consumers are on the branches, rather than evenly distributed over both the branches and the main feeder, as in the paper. A further study indicates that under such conditions, branch protection provides two or three units greater improvement for single-element cutouts, but no greater relative improvement for different devices or different percentages of temporary faults.

Apparently there was a misunderstanding of some of the values given in the paper on the part of Mr. L. G. Smith, which led to his questioning of the validity of two of the conclusions. Mr. Smith indicates that the operating data of the company with which he has been associated shows that 70 per cent of the feeder troubles are restored without a lockout. This operating experience lines up quite well with the values given in Table IV in the appendix of the paper. The values used in the calculations for determining the consumer minutes outage from which the curves were plotted are shown, in Table IV, to line up with this operating practice. This shows that while the reduction of the number of outages by reclosing equipment may be a very high percentage of the total, it does not necessarily indicate the same reduction in consumer minutes outage when related to the whole system. Consumer minutes outage takes into consideration the time it takes to go out and locate the fault, make repairs, and so forth. A large proportion of this time is required for permanent faults even with a high percentage of temporary faults. Single-element line sectionalizing and branch fusing reduce this time as well as decreasing the number of consumers affected. With branch fusing the consumer minutes outage caused by fuse blowing on temporary faults is a very small percentage of the total for the whole line, since only those on the one branch are affected. Therefore, even though reclosing devices on branches do restore service on 70 per cent of the faults without lockouts, the improvement in consumer minutes outage is very small when related to the system as a whole. However, as pointed out by Mr. Craig, the treatment of an individual branch as a unit would show a much greater improvement for reclosing equipment, and this might be sufficient to warrant special attention where some peculiar condition exists on that branch. It might even improve the consumer minutes outage on the whole system, if the percentage of temporary faults on the branch were particularly high as compared to the percentage of temporary faults on the system as a whole.

Mr. B. O. Watkins brings out some of the problems involved in rural electrification. He draws the conclusion that the automatic resetting recloser provides the real answer to the rural problems. As partial substantiation of these conclusions, he cites some data based on circuits employing gaps for lightning protection of the individual transformers. It should be recognized that such a setup has a fictitiously high percentage of temporary faults, since the automatic reclosing devices are called upon to give not only overcurrent protection, but also a part of the function that proper lightning protection should give. A tabulation of the number of successful reclosing operations without lockout should show up well, because the arc across the gaps is very likely to go out, as quickly as the recloser opens the circuit the first time, and is not likely to be re-established. The data presented in the paper are based on a normal system with the better conventional lightning protection, where the reclosing protective devices are not called upon to do double duty in interrupting the accidental plus gap-created short circuits. Therefore, the improvement indicated for reclosing devices in the paper will fall short of the improvement shown by data which include numerous additional operations to clear on the first reclosure arcovers on gaps employed for lightning protection.

Mr. Watkins stresses again the limitation in the number of reclosers that can be connected in series and the difficulty of coordination with the fuses at the transformer, which is brought out in the paper. He gives his idea of an ideal design which would have instantaneous operation on the first opening and then inverse time-current characteristics on subsequent operation. Such a device would have some advantages over the present design, although it would not provide for any increase in the number of devices that could be connected in series. The instantaneous operation on the first opening would retain the advantages of the present design in the preventing of burning down of lines, which is mentioned by Mr. R. O. Loomis when he states, "I have not found a case where conductors burn down when protected by oil circuit reclosers." It would probably have a similar advantage as regards the burning back of gaps employed for lightning protection, which might become excessive more quickly with reclosers having a longer time of operation on the initial opening. Actual operating experience is available only with the rapid opening.

All of these factors should be considered when employing the curves of the paper for the study of the relative value of different types of protective devices, when the characteristics of the devices or the type of the application vary from the assumptions of the paper.

Mr. Craig has presented some additional information and suggestions for applying the data presented in the paper, which should extend its value.

Relays and Breakers for High-Speed Single-Pole Tripping and Reclosing

Discussion of paper 42-35 by S. L. Goldsborough and A. W. Hill, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 77–81.

Eric T. B. Gross (College of the City of New York, New York, N. Y.): In connection with the application of relays acting upon the phase-angle shift between two sets of phase-sequence quantities, it might be of interest to mention that a similar problem arises in the problem to secure correct operation of distance relays in delta-wye connected systems. One of the solutions to this problem makes use of wattmeter-type phase-sequence relays.[1]

Reference

1. Influence of Wye-Delta Transformation on Distance Protection of High-Voltage Systems, E. Gross. *Elektrotechnik und Maschinenbau*, volume 55, 1937, pages 333–8.

Evening Courses at Graduate Levels—a Challenge to Colleges of Engineering

Discussion and author's closure of paper 42-34 by Robin Beach, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 88–94.

P. L. Alger (General Electric Company, Schenectady, N. Y.): This paper is very welcome for its record and advocacy of advanced education for engineers in industry. It is obvious that any four-year or even six-year course in college is only the beginning of an engineer's education. All who aspire to professional standing, therefore, must continue their studies for many years after leaving the halls of the university. Heretofore, most graduates have found it sufficient to learn from the hard knocks of experience and the advice of their associates, supplemented by occasional periods of individual study. It must be expected, however, that very many men in the future will wish to accelerate this process of continued learning, and will welcome the help of experts in so doing.

There are several reasons why the colleges should seize this opportunity for expansion, besides the mere filling of a growing educational need. For one thing these older students, engaged in practical engineering work, will bring to the university definite ideas on the utility of various sorts of knowledge, and numerous problems whose solutions are needed. Thus, the college professors will be jarred out of any academic ruts and stimulated to extend their own knowledge. In this atmosphere research programs will naturally grow up, industry support for them will develop, opportunities for student employment will arise, and undergraduate education will become more realistic.

Also teaching practicing engineers how better to perform their daily work is a public service of immediate economic value. The tuition fees received in consequence are likely to be multiplied many times in the college income, merely because of the community of interest and co-operative spirit thus created between the university and the town.

Any step that lessens the time between spending money for education and reaping its rewards reduces over-all costs and strengthens the financial position of the college. To advocate as some do that the government finance large numbers of students for a fifth or sixth year of college residence is, therefore, economically unsound, and, by the same token, evening courses for graduates are economically sound. They open the way for many young men to learn things they could not otherwise afford to study.

I believe that the future trend of these evening courses will be to include many more civic and cultural subjects that will make the engineer a better citizen, a man of affairs rather than a technician. It is only after an engineer has begun to carry business and family responsibilities that he can put to use a knowledge of law and accounting, or of history and government, or indulge his tastes for art. He should study these things after college, therefore, rather than postpone his technical courses on their account. The largest field of future service for the colleges may be the civic and cultural education of mature and busy men. If the colleges undertake this work, the professors will gain a far better understanding of business economics, and the business men will lose some of their commercialism, to the great benefit of us all.

F. R. Stansel (Bell Telephone Laboratories, Inc., New York, N. Y.): As a recipient of the doctor of electrical engineering degree from Professor Beach's institution, I wish to express my gratitude for the opportunities afforded to me by the Polytechnic Institute of Brooklyn and to add a few observations from the student's point of view.

A year or two after graduation many young engineers find themselves faced with a dilemma. They realize that in any of the fast-growing fields of engineering, the basic training acquired in their four years of undergraduate study is insufficient, but they often feel that it is impractical for them to now leave their jobs and go back to school for advanced training. Besides the obvious reasons of money and family relations, there are many other considerations including the realization that industry itself is a vast school. The engineer while realizing the need of more training in the basic principles for his future advancement, wonders if the training in engineering practice offered by industry is not of more immediate importance. Evening graduate courses of the type outlined by Professor Beach offer a solution to this dilemma, allowing the student to broaden his knowledge of the fundamentals and, at the same time, retain his position in industry.

From the financial angle, evening graduate study is peculiarly advantageous to the young engineer. The cost is automatically spread over several years without the necessity of seeking loans or scholarships, and the engineer is able to retain a job much more remunerative than part-time employment which he could obtain as a full-time student.

Professor Beach referred to evening graduate students as differing "from the conventional graduate student of the day session in a number of essential characteristics." One of the characteristics he refrained from mentioning, however, is the student's fatigue from having already accomplished a full day's work. Due to this fatigue, problems of classroom ventilation and illumination assume major proportions. Another great help in this matter of fatigue would be the more wide-spread use of mimeographed notes covering the more complicated mathematical formulas, and particularly drawings, thus relieving the student from acting as a transcribing machine, and allowing him to concentrate on understanding the material placed before him.

Finally, the picture of evening graduate study would be incomplete without a mention of one other group of persons—the wives and girl friends of the students. Many a graduate schedule has been completed only because of the unselfish willingness on the part of the student's wife to give up the companionship of her husband for one and frequently more evenings each week.

Robin Beach: The unusually large attendance at this meeting, as well as the active participation in discussion, both by members of industry and of the teaching profession, have clearly demonstrated the wide-spread interest in this field of graduate education through evening courses.

The general approbation of the progress already made by a number of engineering colleges in graduate study through evening programs of courses was indeed encouraging, and it should serve as a stimulus to those who contemplate entering this challenging field in the near future.

The author takes this opportunity to express his appreciation of the thought which has been directed to his paper by all of those who contributed so interestingly and so richly to this educational session.

Utilization Voltages

Discussion and author's closure of paper 42-69 by H. P. Seelye, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 147–51.

W. J. Russell (Westinghouse Electric and Manufacturing Company, Mansfield, Ohio): Mr. Seelye has made a very interesting presentation of voltage-variation data, which should be of considerable value to the industry. As manufacturers of domestic appliances, we would like to introduce a short discussion of the effects of voltage variation on small appliances (small portable devices).

The effect of voltage variation within reasonable limits on small appliances is, as a rule, of little practical concern. According to Mr. Seelye's investigation, the voltage range for appliance use is approximately 110–125 volts. Since most small appliances are now designed for 115 volts, the cases of underwattage will be less than those of overwattage. It is the former that is the basis for most customer complaints.

The trend toward automatic or thermostatically controlled devices has minimized the effect of voltage fluctuation. With automatic temperature control, sufficient wattage may be used to provide for proper operation under any normal voltage condition without danger of overheating on the higher voltages.

Nonautomatic devices are usually of lower wattage, and voltage variations are of more concern. However, we have found from long experience that cooking operations may be slowed or accelerated without harmful results within the range of known voltage variations.

The allowable wattage for standard wall receptacles is limited to 1,650, and, on some of the larger portable appliances, such as roasters, broilers, and double hotplates, this has caused trouble with slow operation when low voltages are encountered. However, improvements in efficiency have been made in the past few years which have largely overcome this trouble within the normal voltage range.

In the design of all appliances, sufficient factor of safety is included to take care of voltage variations so that they will have little effect on the life of the device. Life tests on our devices are conducted on an average voltage of 125.

We believe that with the existing voltage range, as shown by Mr. Seelye's study, we should continue to use 115 volts as the design voltage for small appliances, inasmuch as with this voltage the slowing effects of underwattage are not serious, and the overwattage can be taken care of by proper design. An increase in the design voltage to $117^{1}/_{2}$ or 120 volts at the present time would increase the field complaints, particularly on nonautomatic devices, due to slow heating in cases where the service voltage is on the low side of the range.

C. A. Powel (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Seelye's paper is quite timely in view of the work being done by the joint EEI-NEMA committee on preferred voltage ratings on the revision of the 1930 report. His plea for a general conception of utilization voltages as a band rather than a fixed value should command attention. In the past we have spoken of "nominal system voltages" and "rated circuit voltages"; it would seem desirable to revise this idea and speak only of a "nominal system voltage," which would be expressed as a band or would be understood to constitute a band, leaving the term "rated voltage" to be applied only to the equipment used on a system of the corresponding nominal voltage. There appears to be no good reason for having a single voltage value assigned to a system, particularly since it is admitted that of necessity we cannot select any such value which will be correct for all parts of the system of a given voltage class.

Utilization appliances must, of necessity, be given a single voltage rating at which their performance is specified, but, to a large extent, they are self-compensating and will give quite satisfactory performance over a fairly wide range of voltage variation. The life of the appliances is also not noticeably affected by reasonable voltage variations.

In regard to the lamps, it is desirable to keep down the number of special lamps which have a relatively small market. Orders for small quantities of these special lamps are quite burdensome to the manufacturers in that the stoppage and adjustment of equipment required for their manufacture interrupts and delays the production of higher-demand units. Moreover, they constitute a problem in that stocks have to be maintained in most cities and towns.

A. H. Kidder (Philadelphia Electric Company, Philadelphia, Pa.): Those who are interested in utilization voltage problems are especially indebted to Mr. Seelye for his progressive contributions and for his preparing so fine a review of the work. There are only two points which I think may deserve more consideration than has been given in the paper. The first is the effect of emergency operations upon supply voltage, and the second is the problem of the odd ratio.

EMERGENCY VOLTAGE

Provision for motors to start at 90 volts was made a part of both the maximum utilization voltage spread and the preferred voltage spread for residential customers. The need for the provision is not self-evident, and I do not recall mention of it in the paper, for the summary shows the maximum voltage drops in normal operations alone, as reported by the 14 companies. With the increasing dependence of customers upon continuous electricity supply, the time has come when emergency operations generally satisfy the need for at least partial service restoration, long before it is possible to complete the repairs necessary to restore normal conditions. When the supply to a distribution circuit has been interrupted, therefore, it is usual practice to transfer the load to adjacent circuits, if possible, and to supply it in the meantime, despite a possibility of low voltage.

To illustrate, assume that the normal voltage drops are the minimum values given in the author's table; that is, $2^{1}/_{2}$ per cent in the transformer, 2 per cent in the secondary, and 7.5 per cent total. If it is then assumed that the last transformer has failed on the circuit illustrated in Figure 3 of the paper, and its load has been transferred temporarily to the adjacent transformer, it will be seen that the load on the adjacent transformer will have doubled while both the circuit length and the load will have become three times normal in the secondary branch that is used to pick up the load which had been lost. Under this emergency condition, the voltage drop in the transformer would increase to 5 per cent and that in the secondary would increase to 18 per cent (2 per cent $\times 3 \times 3$), or an overall increase of $18^{1}/_{2}$ per cent above the $7^{1}/_{2}$ per cent normal voltage drop. In a system designed for 125 volts maximum, the refrigerator or oil-burner motor whose thermostat calls upon it to start under such conditions would have not much more than 90 volts. There are, of course, many possible examples, but this one is thought to be sufficient to show that secondary design for greater voltage drop probably requires provisions for dividing the load of the circuit in trouble between more than one adjacent circuit, if the load at the time is sufficient to require the additional emergency load transfers. Because urban distribution designs more often observe the limitations imposed by emergency rather than normal operations, it is quite natural that the normal voltage spread in urban distribution, and particularly in networks, may be somewhat less than in suburban distribution.

ODD-RATIO UTILIZATION VOLTAGES

Figure 4 of the paper illustrates very well the nature of voltage spread for even-ratio equipment under normal operating conditions at even-ratio voltage. It does not, however, represent the much wider range of voltages which is faced by even-ratio equipment on circuits which supply either even or odd-ratio voltage—a situation the paper mentioned very briefly in the comments on 240-volt equipment. Motors are now almost the only utilization equipment which has not been made altogether adaptable to odd-ratio supply. Since the odd-ratio circuit is not yet used extensively outside of urban areas, it may be well to consider the problem in terms of the urban voltage spread of Figure 4, which needs only to be extended, in the accompanying chart, to illustrate the place in which even-ratio equipment finds itself on the odd-ratio urban circuit. For this purpose the reference point is taken to be the average voltage for the even-ratio connection on the odd-ratio circuit, and all deviations from that average are expressed as percentages.

From the above illustration it will be seen that the even-ratio motor, which is required to operate interchangeably on either even-ratio or odd-ratio voltage, must be prepared to meet satisfactorily a voltage range of +5 per cent to −18 per cent or a range of voltages up to 1.28 times the minimum odd-ratio voltage in urban distribution of the characteristics represented by Figure 4. This range is about midway between the 1.24 range, from 197 to 245 volts *preferred*, and the 1.30 range, from 193 to 250 volts maximum which the author mentions as being considered for such motors. All of these ranges are greater than that of 1.22 between the 90 per cent and 110 per cent limits established in the American Standards for Rotating Electrical Machinery, as being the allowable variation from rated voltage for motors. The American Standards must be revised, therefore, to permit use of any range greater than 1.22, no matter how carefully the rated voltage may be selected. Provision in the American Standards, for a greater voltage range (and retention of the standard "208-volt" single-phase motor line for difficult odd-ratio situations) seems to be the only practical solution to the odd-ratio problem in single-phase motors for which some such compromise solution seems to be inevitable.

On the other hand, discussion of the odd-ratio problem in three-phase motors has led to the suggestion of a possibility that the speed-torque-excitation characteristics of such a motor can be made to be exactly reproducible when it is supplied from any three-phase circuit connection that happens to be at hand, be it even or odd-ratio volt-

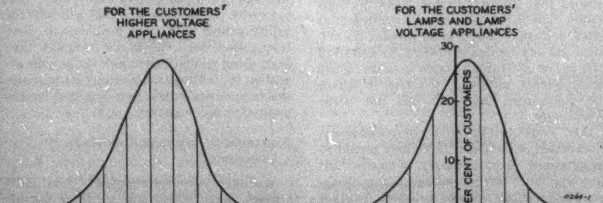


Figure 1. Illustrative distribution of voltages supplied by the odd-ratio urban circuit

age. It may, therefore, be possible that the odd-ratio problem in three-phase motors is susceptible of an exact, rather than the compromise solution necessary for the single-phase motor. If an exact solution should be found to be practical, there would appear to be no justification for adding any special, approximate, or halfway measures to the present American Standards for three-phase motors.

The proponents of an exact solution for the three-phase motor argue quite convincingly that the differences between even-ratio and odd-ratio voltages in three-phase circuits are due entirely to differences in the coil connections, which could be adjusted almost as easily in a suitable motor as in its supply transformer. They point out that the exact solution can be attained by building the present so-called "208-volt" line of motors with the same number of coils and only two more taps than are standard in the present line of 220-volt three-phase motors. With such connections the motor could be connected so as to have exactly the same performance characteristics on each one of the three possible three-phase secondary distribution voltages: lamp-socket voltage, twice lamp-socket voltage, or odd-ratio (1.73 times lamp-socket voltage), as the case may be.

Such a motor, if practicable, would be seen to include one connection which is so nearly 110 volts as to make it quite probable that the now separate standard 110-volt line of three-phase motors could be discontinued altogether. It would also have the advantage that the American Standards for Allowable Variation from Rated Voltage for Motors would not need to be revised, except possibly for the special case of the single-phase motor. I believe the suggestion may be worthy of more serious consideration than it seems to have received.

Hamilton Treadway (Rural Electrification Administration, Washington, D. C.): The problem of standardization of utilization voltages presented by the author becomes critical in the development of large rural systems spread over several hundred miles. In such systems the spread in voltage must necessarily be wider than in the urban and suburban communities or even in the nearby high-density rural area. As the rural area becomes thinner, the economic gains to be realized by closer voltage control are much smaller. The difficulties in standardization of utilization voltages consequently are increased.

This suggests another approach to the problem; namely, voltage co-ordination. By voltage co-ordination is meant co-ordination of the appliance rating voltage with the voltage prevailing at the consumer's outlets. Approaching the problem from this angle would suggest perhaps three or four utilization voltage spreads to be used by the manufacturer, for example 105 to 115; 110 to 120; 115 to 125; and 120 to 130. This may require a change in our fundamental thinking and may place a greater burden on the supplier of electric service to advise the consumer of the proper appliance voltage rating to be used on his premises. Whatever change may be required in this direction, it is probable that voltage co-ordination lends itself more readily to supplying adequate and efficient service than does the establishment of a single utilization voltage spread.

In light of the author's figures on over-all voltage spread, it appears that the trend toward the purchase of 120-volt lamps might well be reversed if economic light production is to be obtained by the consumer. Undoubtedly the real reason for the past and present trend is not so much due to a difference in the schools of thought on the proper voltage at which to operate incandescent lamps as to a lack of information in the hands of the user on the most economical voltage at which the incandescent lamp should be operated. There is likewise a similar lack of knowledge on the part of the consumer on the most economical voltage at which to operate other appliances. A program of system voltage co-ordination is again the suggested solution.

Approaching the problem of utilization voltages from the voltage co-ordination view should permit the manufacturers to design more efficient appliances for consumer use. It should also reduce investment in lower consumer density areas without impairing the quality of service rendered. Of course the need for a narrowing of the utilization voltage spread throughout an operating system and particularly between systems cannot be overemphasized.

Howard P. Seelye: The fact that the discussions of this paper which have been presented contain very little criticism would seem to indicate quite general agreement on the main ideas offered, namely, the need for standardization of utilization voltages, the desirability of considering the voltage standard as a band of voltages rather than a single voltage, and the specific values suggested for that band. Only one, Mr. Treadway's, proposes the very different approach of setting up a multiplicity of bands. As a general practice, however, this would seem to be a step backward, adding confusion and hence cost in manufacturing and distributing appliances. The survey of operating systems and the fact that service conditions are, in general, quite satisfactory does not indicate that any such step would be justified.

Mr. Kidder's analysis of the operation of even-ratio equipment on odd-ratio systems is logical but does not take sufficient account of the fact that odd-ratio systems, being essentially three-phase, four-wire, are almost entirely confined to areas of high load density where the voltage is well regulated. The minimum voltage is likely to be 115 volts or more, giving at least 200 volts for the three-phase motors. Past practice has shown that 220-volt motors have, in most cases, had little difficulty in operating at such voltages. There is at present no evidence of any tendency toward the spread of the use of odd-ratio systems to more lightly loaded areas, although, of couse, it cannot be guaranteed that this will not occur eventually.

Mr. Powel brings out a point which was mentioned in the paper but which should be emphasized. It is necessary, of course, that any appliance have a voltage rating, designated as a single voltage, to which its other ratings and characteristics are referred. Such ratings may be different for different appliances. On the other hand, it is highly desirable for general use that a single name be given to the whole class of appliances which will correspond with that of the system on which they are used—in other words a *nominal* voltage, which could well be 120 volts in view of the preponderance of the 120-volt lamps. This nominal voltage would merely designate the standard band or spread of voltages and should not be considered as a mid-point, which would be actually 117$^1/_2$, if the proposed spread is adopted. If voltage *ratings* for appliances can be made to coincide with the nominal voltage, possibly by giving a different tolerance for plus voltage than for minus, confusion would be avoided, but if this is not practicable, the nominal voltage should be emphasized for general use, the rating being relegated to an auxiliary position for technical purposes.

Electric-Power Distribution Systems in Wartime
(42-60)

Underground Distribution Systems in Wartime
(42-62)

Power Supply to Distribution Substations in Wartime (42-42)

Distribution Substations and Wartime Necessities
(42-61)

Overhead Distribution Systems in Wartime
(42-45)

Discussion and authors' closures of paper 42-60 by Philip Sporn, paper 42-62 by L. R. Gaty, paper 42-42 by H. P. St. Clair, paper 42-61 by F. C. Poage and M. W. Reid, and paper 42-45 by Harold Cole; presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 105–07; 107–12; 112–17; 117–22; 123–6.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): I can assure you that Mr. Sporn's paper covers many ideas that are really worth-while and that will help the utility people with their problems in these trying wartimes. We in our company have been using many of the methods he calls attention to. Appreciable reduction in material requirements has been obtained by the methods I will mention.

SACRIFICE IN VOLTAGE RENDERED TO CUSTOMER

We have been maintaining a very good voltage range which was a rather narrow band in most cases, on a peacetime basis, but we found that we could let the voltage drift in many cases and still render *usable* voltage to the consumers. Actually, in many cases we changed the transformer taps at the industrial consumer's plant to raise the voltage to compensate for the increased load on *his* distribution system. This is not necessary normally and, in some cases, does raise the light-load voltage somewhat, thus providing a wider band of voltage. Entirely satisfactory performance has resulted from these changes, and system changes have not been required, nor have any materials been required.

On the distribution feeders with feeder regulators at the supplying substation, we have in the past been increasing their voltage range from five to 10 per cent to stay within a narrow band rendered the consumers, but now, in some cases, we are not changing the regulators for voltage reasons and are letting the voltage drift a little during heavy load periods.

In our state (Pennsylvania) we have certain voltage limits prescribed by the Pennsylvania Public Utility Commission. We are well within these limits and in peacetime have spent money and performed construction and used materials to keep the very narrow band of voltage even above such requirements. Now we are permitting the voltage to approach the commission requirements before we use materials to improve service.

Relative to loading of equipment we have been crowding our equipment for the last few years and have been doing considerable research work to discover means of crowding it still further. We have determined individual ratings for power transformer banks in transmission substations and in distribution substations, and also for induction feeder regulators, both indoor and outdoor, and we find that a considerable increase in capacity can be obtained by operating these pieces of equipment on the thermal basis governed by the particular load cycle. We discovered that this would relieve our material requirements appreciably, as well as save the man-hours required to design and construct new installations. Ventilation is a field well worth exploring, and our experience has been that this is well worth any research work that you do. It is surprising how little *air movement* is required to get an appreciable increase in capacity. We also find that you will probably have to do a considerable amount of local research, due to the particular conditions surrounding your equipment, such as age, conditions of equipment, natural ventilation, indoor, outdoor, load cycle, and so forth.

Similar investigations have been going on on overhead lines and underground cables, and appreciable increases have been developed.

EMERGENCY CONDITIONS

Naturally, the same amount of reserve and emergency cannot be provided in wartimes as in peacetimes, because of the scarcity of materials and man power, as well as the lack of time to provide said reserve. Normally, of course, all of us try to render good service under the most extreme adverse conditions, but the ability to do so has gone "since Pearl Harbor."

To build two lines and provide two transformer banks to industrial customers, in most cases does not appear to be justified, and I suspect you would have a hard time getting an approved priority application to provide for this reserve. However, this does not mean that we should sacrifice on the quality of design or the strength built into the installation. Because of the fact that there is some reduction in reserve, the design should be at least equal to the good old peacetime quality if possible.

I seriously question the advisability of providing reserve, in many cases, for breakdowns which can be repaired in a relatively short time, such as a transmission-line burndown, which can be repaired in a matter of 8, 24, or 36 hours. Of course, such cases must be tempered by the business of the customer, for an interruption to a plant which merely curtails production, where production can be resumed practically immediately upon restoration of power is one thing, whereas an interruption to a plant where curtailment means considerable loss of material and requires a large amount of time to restore production, or endangers the lives of a large number of employees (such as coal mines) might require some degree of reserve over and above others.

MATERIALS

The utilization of different materials from what you would normally use is necessary, of course. We must use wood structures if we cannot get steel; we must use different size and type transformers if we cannot get what we want to have. We must use the different type and styles of circuit breakers with necessary control interlocking and sequential relaying schemes to keep the duties within the capacity of the breakers; we must use fuses instead of breakers if we don't have the breakers. By and large, we must use materials that we have or can get, and must not make our material requirements a burden on the already overburdened material situation. Some suggestions have been made that the excess materials (if any) among utility companies and others be pooled and used where most urgently needed. This, of course, is merely good sound horse sense necessary to help win the war, which we must do.

PLANNING

All planners, particularly in utility companies, must plan the system in an entirely different manner, generally along the lines outlined above, and our company has been doing this for quite a while, and we find that it is satisfactory. Of course, we are going to get complaints here and there due to outages, low voltage, flickers, and so forth. Such complaints will just have to be tolerated where their correction cannot be justified under present conditions. Of course, too many complaints or too serious a complaint must be taken care of.

We may have to tolerate voltage flickers to a greater degree, even to the point where they are slightly objectionable to lighting consumers, but if too many objections are made, the voltage flickers will have to be corrected.

LOAD SHIFTING

Load shifting to ease the burden of demands on equipment is a possibility, and individual systems will have to investigate and see what can be done. Obviously spreading the same loading over twice the time reduces it in half, but this is merely academic and not practical. You can really shift the peak load on an electric utility system by staggering working hours in different manufacturing plants, or by shifting some of the day work to night work. Curtailment of nonessential loads is also a fertile field which, I believe, will be cultivated before the war is won.

While these comments are directed to Mr. Sporn's paper, some of them apply to companion papers presented at this meeting, particularly those of Messrs. St. Clair and Cole.

CONSERVING EQUIPMENT NECESSARY

I was particularly interested in many points brought out in Mr. St. Clair's paper relative to loading the equipment to the absolute safe thermal limit, and even beyond, to some extent. In these wartimes we have to sacrifice some life of the equipment rather than to replace it with larger equipment, or to consume materials needed elsewhere in order to manufacture this larger equipment.

Our company has been doing many of the things Mr. St. Clair mentioned, and we have had particularly good success with them. We are just purchasing a 3,000-kva portable substation, 22/4 kv, for use in emergency and for maintenance and repairs. The secondary of this transformer has various voltage combinations which will permit using it on many installations. Actually, as a result of this portable substation being available soon, we have curtailed the amount of transformer capacity installed in some of our newer semirural districts—all at an appreciable savings in capital investment and in savings in materials.

SWITCHING EQUIPMENT

I can assure you that considerable economies in investment and materials will result from the selective relaying, wherein a group breaker is tripped instead of the replacing of individual feeder breakers with larger ones. We made a very careful analysis of this some years ago, and the evidence disclosed that there were very rare instances wherein the failures occurred at locations which would produce short circuits in excess of the small feeder breaker capacity, and, therefore, such group breaker tripping would not affect service adversely except in very, very rare instances. We have, therefore, been employing this practice with marked success for a number of years, and just last year we finally completed this selective relaying scheme in our four-kilovolt distribution substations. In fact, I am very glad we did, for I doubt if we could obtain larger breakers.

We have also adopted sectionalizing tripping for larger transmission substations, which means that the bus tie breaker or breakers are tripped first and practically instantaneously, and then the individual line breakers are well within their capacity. This latter feature has been in force on our system for 10 or 12 years.

Relative to tapping transmission lines with distribution substations, we believe that this is an excellent plan for curtailing expense and material requirements. We have just recently installed a 4,000-kva, 66/4-kv distribution substation and connected it onto our 66-kv transmission ring, because we had no 22-kv line anywhere near the proper location for the new substation. This has worked out quite satisfactorily, even though we did encounter considerable opposition in our own company to connecting such loads onto the so-called "sacred" transmission ring. However, economies justified it, and we had to proceed with it.

Certain benefits, of course, have resulted from the revising of our operating practices, particularly with reference to taking equipment out of service. We just do not take it out of service now until we absolutely have to do some work on it.

One comment on Figure 3 might be of interest. A modification of this would be for the step-down bank and the four-kilovolt bus to be in the same location rather than 0.7 mile apart. We have several of these substations, one at 66 kv, as referred to earlier, and several relatively similar to this at 22 kv. We have installed on the high-voltage bus a single-phase grounding disconnecting switch, which is actuated by the differential relays and upon their operation, actually places a ground on the high-voltage system. This is done, because a fault on the low side of the bank, but not within the area of the differential protection, might be of such low magnitude, particularly where the bank is small, that the high-voltage transmission-line relays at the nearby substation would not get sufficient current to operate. We developed this switching method in our own laboratory and made several trial installations and put it through its paces. We found it quite satisfactory and have been using it for some time.

Harold Cole (The Detroit Edison Company, Detroit Mich.): I want to describe a method of reinforcing a subtransmission system to enable it to supply increasing loads, a method which The Detroit Edison Company has recently applied to good advantage, and which may have application to other systems. We are just completing the changeover of the transmission network serving nearly half of our total service area from 24-kv to 40-kv operation. This changeover is being made at a very low cost, considering the increased capacity obtained.

The conditions were, of course, very favorable to the adoption of this scheme. The area involved, the so-called "Thumb" district of Michigan, has a low-load density, and the load growth will probably be slow. The distance from power sources to the extreme limits of the area are over one hundred miles, and voltage regulation had been a serious problem for several years. All substation transformers in the area were connected delta-delta, and about the only expense involved in changing to a wye-delta hookup was the cost of changing bushings. Circuit breakers at switching stations in the area had to have new bushings and be modified to obtain higher interrupting capacity and better clearances. Very little line reinsulation was found necessary for operating at the higher potential. Autotransformers were purchased to step up the voltage at the points of connection between the 24-kv and 40-kv system. These have a low impedance, so that the total over-all regulation of transformer and line will be improved as much as possible over the regulation of the lower voltage line alone.

As a result of this change, the lines in this area can carry more than twice the load they formerly carried before reaching limits of voltage regulation. The cost of making the change is much less than any other of the possible expedients studied, although some of the other expedients would have made possible a greater increase in load. If it becomes necessary later to reinforce the supply to this area, however, the higher subtransmission voltage will be very helpful in keeping line costs to a minimum.

We are now planning to use this same method of increasing line capacity to take care of new defense loads coming on in several suburban areas where distances from supply sources are too great for economical supply at the 24-kv level. The savings in the use of vital materials are obviously large, and if, as in our case, ways can be seen to fit the autotransformers required into the future changeover of a large portion of the suburban system to higher voltage levels, the scheme has great possibilities in contributing to the war effort without any great sacrifice in ultimate economy.

G. H. Fiedler (Rochester Gas and Electric Corporation, Rochester, N. Y.): I have reviewed Mr. Cole's paper on "Overhead Distribution Systems in Wartime" which I think is very timely.

We have found that a considerable amount of conductor could be saved by running two-wire services rather than three-wire, unless the customer has equipment which actually requires 240 volts. It has been our policy for many years to install three-wire circuits if a residence had four or more lighting circuits. We are still encouraging the installation of a three-wire riser and distribution panel for these customers but are extending only two service wires at present and installing two-wire meters. In some instances, the extension of the third secondary wire is saved, and to date we know that several thousand feet of conductor have been conserved on services alone. Very few voltage complaints have resulted and we intend to continue this practice until critical conditions are relieved.

G. S. Van Antwerp (Philadelphia Electric Company, Philadelphia, Pa.): Mr. Cole is to be congratulated for having stated clearly and concisely the principles that must be observed in extending and maintaining overhead distribution systems with smaller amounts of critical materials. It is difficult to believe that anyone can object to using any or all of these methods where at all applicable to his individual problems. With a reduced and limited amount of material available, either new or reclaimed, there is set up a real challenge to line departments.

Should we leave customers "out of service," because there are no bright and shiny arm braces, guy clamps, secondary racks, splicing connectors, guy rods available? Or shall we straighten the bent and dirty braces, hand-serve the stranded guy wire, use a wood bracket or arm in place of the rack, make up a wrapped joint for the connector, forget the guy rod and run the guy wire to a buried dead man?

It is a fine feeling to get a supervisory group together, enlist their enthusiasm, patriotism, loyalty, and co-operation to carry out a program such as this. But what a different reaction one gets "near the belt line," after finding that the fine words and elaborate procedures still build scrap piles of reusable materials—mounds of good intentions gone wrong.

Sell the supervisory group, yes, but that is not enough. Before positive results can be achieved, the man on the job, the layout man, the estimator, the storekeeper, the foreman, the lineman, and the lowly helper must be with you and Uncle Sam. That's where they want to be—helping; so give them a chance. Not only will they show you more ways of saving materials, but I suspect they will fight for reclaimed materials to—

Keep those kilowatts rolling!

Floyd M. Fuller (Pennsylvania Power and Light Company, Allentown, Pa.): To a distribution engineer of an operating public utility, the papers presented are very interesting reading, as describing current good practice which, no doubt, is being followed by many engineers.

All distribution engineers know, either from experience or from rules promulgated by governmental bodies, the limitations in regulation, and from their executives, the continuity of service required. In most cases limits of construction are imposed either by regulatory bodies or "codes" which, in many cases, are now used as the standard of construction. These restrictions have been imposed upon the distribution engineer in his practices, and we all know how difficult it is to change such codes and regulations.

As an organization why cannot we take some action and take it promptly to obtain a relaxation of present requirements. For example, in the Commonwealth of Pennsylvania voltage regulation must not exceed plus or minus five per cent of the standard voltage. If this is exceeded, and all of us in the industry know it may be exceeded to a certain extent, why not have the regulations waived for the duration, as long as the customer can live with the regulations. Another example is guying over railroad and telephone facilities where electric lines, except for the lower voltages, are able to be installed with guys having a factory of safety of three, which in our estimation is excessive.

For improving the voltage, Mr. Cole, in his paper, very wisely stresses the need of saving material in new construction and the use of regulators, boosters, and capacitors. He might also to good advantage have brought out in his paper the benefits accruing from increasing the voltage. In the case of changing from a three-wire 2,300-volt to a four-wire 4,000-volt system using a common neutral (in many cases the existing secondary neutral), very little material will be required, in fact a lightning arrester and fuse cutout will be salvaged from each single-phase transformer installation and this increase in capacity may eliminate the need of replacing existing conductors with larger ones to provide additional capacity.

Similarly increased capacities may be obtained by changing from 6,900, three wire, three phase to 12,000 volt four wire, three phase without replacing transformers. It may also be advisable in certain cases to obtain the additional capacity by converting a system from 2,300 or 4,000 to 12,000 volts.

Another way to conserve in material is in street lighting: where the work is necessary and justifiable. By the installation of modern luminaires, having double the efficiency of the old equipment and using higher mounting heights, one-half the number of lights now existing on the boulevard standards can be installed, and the old obsolete equipment salvaged for defense.

The distribution engineer can and will change his practices in the interest of the conservation of material, but he could go much further in such conservation, if the walls of regulatory bodies and the requirements of reliability of service are lowered for the duration of war. An earnest request is made at this time that suitable action be taken by our officers with this in view.

H. E. McDowell (nonmember; Ebasco Services, Inc., New York, N. Y.): In the several papers, pages have been devoted to stating the problem and the conditions that are responsible. Simply stated: Many of our systems are short of capacity in certain respects or locations. There is a shortage of materials. Hence, the utilities will have to get themselves out of their predicament as best they can.

Bench Marks

Occasionally, one hears about the bold and courageous steps that must be taken to meet the present emergency. Without any desire to disparage the thought, it might be considered that many, if not most, of the measures that have been suggested will closely parallel practices that are characteristic of companies whose finances never have permitted gold-plate or chromium trim. Bold practices in the eyes of some are but customary in the eyes of others.

Select Plans That Have Multiple Advantages

It should be obvious that the times call for extreme simplification. Further, it is not too late to work toward a higher degree of standardization. This refers to practices, basic designs, flexibility of application, rather than to detailed specifications.

Whereas this discussion represents no attempt to boost the fortunes of the manufacturers of capacitors, it is worthy of consideration that the capacitor presently is the most useful article for increasing system capacity, improving voltage, or providing load relief that can be obtained on short delivery. It carries the advantages of *multiple uses*, referred to above, as well as the benefits of mobility or salvageability, means of reducing system losses—all being things that you are familiar with but which you may not have added up. It is well nigh axiomatic that a company cannot make a mistake in buying somewhat too many capacitors so long as its load is growing. They unquestionably have a great variety of uses.

A recent example that will illustrate the point is that of a company short of generating capacity, this being due in part to lack of reactive kilovolt-ampere capacity in its generators to supply lagging components of loads, together with the necessary reactive required to import available power from neighboring systems. In order to release this "captive" kilowatt capacity, amounting to some 15,000 to 20,000 kw, plans were made to install 35,000 kva of capacitors. At the same time, load growth generally had been such as to require load relief on approximately twenty four-kilovolt feeders. This would involve under usual practices a considerable construction program, including new feeders and allied improvements. The installation of capacitors, which were fully justified by virtue of salvaged generating capacity, so altered the program for distribution-system construction that but one new feeder became necessary, the remaining program consisting of shifting feeder regulators, installation of automatic switching on certain groups of capacitors, and other minor improvements. Cash required for the distribution-system program, exclusive of the capacitors which were justified by reason of generating capacity salvaged, was a fraction of that which otherwise would have been required. The saving in use of critical materials was in like proportion.

Another case in point involved the installation of condensers, capacitors, or both, to strengthen transmission channels to a large section of the company's territory. Choice lay between 15,000 kva of condenser capacity, 15,000 kva of capacitors, or some combination of the two. The selection made was one 5,000-kva condenser and 10,000 kva of capacitors, most of the capacitors being installed on distribution circuits (voltage regulation being largely controlled by existing synchronous condensers). The net result so far as the distribution systems were concerned was to obviate any general improvements to take care of the substantial load increases experienced.

Make Due Allowance for Flexibility in Planning

Planning with an eye to future changes or growth always has been smart—how much more so when conditions are changing with such rapidity now! This refers not to overbuilding but to:

1. Provision of ample ground space (usually a proportionately small item of cost).
2. Flexibility of arrangement of structures, circuits, and so forth, for easy rearrangement.
3. Mobility of equipment The accomplishment of these objectives is a matter of skill rather than of free use of money.

Maximum Use of Transformers

The writer questions whether due emphasis has been placed upon the possible benefits to be derived from paralleling distribution transformers as to capacity saving, voltage improvement, or both. The majority of utility companies in this country has been reluctant to adopt this method of operation, but the writer calls to mind a company that has for more than 20 years judiciously, but quite thoroughly, pursued this practice with entire success. This company operates in a metropolitan area and is in the class that at least enjoys a modest amount of "chromium trim."

Considerable discussion has been given to more thorough utilization of substation transformers. The writer calls to mind one company that averaged between 300 and 500 individual transformer shifts each year during a period of several years when load growth was rapid. Perhaps that might be charged to poor judgment or planning, but that particular system always enjoyed an exceedingly high degree of utilization of all of its components.

Elimination of Nonessentials

It is perhaps trite to say that the way to save materials is to exclude nonessentials, but there can be little question that an astounding saving could be made by eliminating from the conventional systems those gadgets that the simplest systems do not have. This reference in the main is to certain classes of apparatus that have for their justification service improvement. It is suggested that a good start on such a policy might be a visit to some of the poorer country cousins.

Salvaging Operations

But few companies have indulged in a comprehensive way in salvaging used materials. Such companies as have done so justify the expense in part as an offset to pensions and by the greater satisfaction and happiness to old employees in being usefully occupied rather than idle. It would seem that all companies in the present circumstances are duty-bound to embark on a thoroughgoing salvage program, and they might well do so with an eye to its available "pension" features.

R. G. Hooke (Public Service Electric and Gas Company, Newark, N. J.): About three weeks ago, at the request of the power group of the New York section of the Institute, I gave a paper on "Wartime Electric-System Planning in a Major War-Industries Area,"[1] and the remarks which I wish to make by way of comment on the papers presented this afternoon are supplementary thereto.

First, as to methods of limiting the short-circuit duties on oil circuit breakers, there are two suggestions. We have for many years resorted to sequential tripping of breakers on substation transformers in cases where the breaker on one side was likely to be overstressed, while the switch on the other side was adequate. More recently, in a particular situation, we have effected a major postponement of investment by adoption of a plan to operate with a tie circuit between two substations, open at one end under normal conditions. This involved two substations each supplied by four or five 13-kv cables and with a tie line between, which under normal conditions is only lightly loaded. The addition of several thousand kilovolt-amperes in war demands to one substation necessitated installation of a new transmission line, which in turn increased the short-circuit duties on the breakers beyond the values considered safe. Since the tie line between the two substations delivered heavy short-circuit currents in either direction, due to the many feeds to each substation from the source, and since it is an important part of the transmission network only when a circuit to one group or the other is unavailable, it was decided that under normal operation it might be left open at one end; in case of a fault, this end would immediately be closed. After the fault, with one line in the network from the source out of service and the tie circuit in, duties on the breakers were just within rating.

There are three schemes for avoiding investment in the circuit breakers which would ordinarily be considered necessary with increasing loads. All of these involve using available breakers with paired connections to them. Substation transformers may be paired on one breaker on each side with disconnecting switches, the arrangement being such that in case of a transformer fault, a defective unit can be disconnected, and the other quickly restored to service. Meanwhile short-time emergency ratings on other available transformer banks would be recognized.

While we feel we have overcome the likelihood of simultaneous outages of open wire circuits on one pole line, due to lightning, we have not found any means of controlling the auto driver who occasionally takes down our poles. It has, therefore, been our policy to provide in any transmission network for complete loss of any pole line. From this it follows that, if new transmission is needed in a network containing two circuits on a pole line as the maximum outage to be provided for, we may save the installation of switches for the new line by pairing the two open wire circuits on one switch at each end. By doing this we have lost nothing in firm capacity to the area, and if disconnecting switches are installed at the terminals of the paired lines, the loss of flexibility in the transmission system is almost negligible.

In other cases we have found it economical to pair on one breaker at each end small-capacity transmission cables, when a new circuit to the substation is required, and when the capacity of the new circuit is approximately equal to the sum of the capacities of the two lines to be paired. This same policy has been followed in situations where added load in future is expected to be handled by an increase in transmission voltage, thus making it desirable to prevent installation of more breakers than will be needed when the voltage change comes about.

In the matter of investment in transformers, our experience like that of most other companies, has been that failures almost never occur, and that, consequently, every means should be employed to secure higher average use of the units without jeopardizing service, in case one should be unavailable for a brief time. About two years ago we accepted emergency ratings based on 115 degrees centigrade maximum hot-spot temperature. We have also investigated each substation to determine the frequency of high loads and established a "probability allowance factor" for transformer ratings, based on the relative infrequency of occurrence of high peak loads. For example, in a residential area that peak load may occur only on Christmas or New Year's Eve. In such a situation, the probability of concurrence of peak load and transformer outage is very remote. We, therefore, consider that an increased emergency rating is justifiable, recognizing that if a fault should occur at the peak time, we would abnormally impair the life expectancy of the transformer.

We are very much interested in this matter of equipment life and have recently carried out some studies for transmission, similar to those discussed by Mr. St. Clair for transformers. The problem, of course, is to balance the annual cost of new investment against the increased maintenance costs and retirement charges which will result from emergency operation at higher temperatures. It will be found that, in a great many cases, the investment required, if operating temperatures are to be held down to present accepted values, is far more costly than is the higher maintenance and retirement cost which results from higher temperatures and shorter life.

In the transmission system, many of our circuits are limited in capacity by short sections of cable at the ends. Where the lines are short so that voltage drop is not a problem, we have found it highly economical to increase the capacity by either replacing cable sections with cables of larger size, or by putting in additional cables and pairing them at the ends with the present ones, so that one open wire circuit may have two cables in parallel wherever underground construction is required.

By following these expedients and many of the others mentioned in the papers here this afternoon, we have since 1938 expanded our system to meet a 37 per cent increase in peak demand at a total over-all cost from coal pile to customer's meter approximately equal to what, 15 years ago, was considered a reasonable charge per kilowatt for a new turbogenerator.

REFERENCE

1. WARTIME ELECTRIC-SYSTEM PLANNING, R. G. Hooke, E. C. Plant. ELECTRICAL ENGINEERING, volume 61, April 1942, pages 192–6.

E. V. Sayles (The Commonwealth and Southern Corporation, Jackson, Mich.): The five papers dealing with the effect of wartime construction and operation on electric distribution systems should prove to be of much value, if reviewed and studied by engineers charged with the design and operation of electric systems. While much of the material presented constitutes a review of practices, some of which have long been in common use by some companies, it is doubtful if all companies have employed all of these suggestions to the fullest extent. Detailed study of these papers in the light of the present wartime status of the country should unquestionably be helpful.

It would appear that the greatest effect of war on construction programs for transmission and distribution systems is to change us from our normal basis of "economic design" to a new basis of "material conservation." Efforts to conserve materials can be carried to great lengths, and there is little question but that all construction should be carefully reviewed to insure the minimum use of critical materials. In the case of transmission and distribution circuits, this may result in the use of smaller conductors and greater energy losses than would be permitted from an economic standpoint. However, reduction in conductor sizes cannot be carried too far, because voltage drop immediately assumes control, although in many instances voltages may be permitted to be just a little bit lower "for the duration" than might otherwise be considered proper.

With the industry now confronted with carrying greater loads on electric distribution systems, one factor of principle importance—perhaps of greatest importance—is the inherent capacity built into distribution systems for the purpose of providing duplicate service to principal load centers, or for carrying these loads when certain normal facilities fail. For example, there are occasional primary distribution circuits supplied from one substation, which during emergencies can be served from another primary circuit supplied from another substation. In some cases there has been a tendency to keep these circuits lightly loaded in order that they could be used as tie circuits between these substations, in the event of failure of power supply to the distribution substations. While this is a frequent practice, it would seem that transmission-and-distribution-substation design should be such as to permit greater confidence in it, thereby releasing primary-circuit capacity for the supplying of greater loads. It will be recognized that this general philosophy can readily be applied all along the line, starting from the low-voltage secondaries up through the transmission systems. Perhaps a relatively small expenditure to improve the reliability of a single transmission circuit may obviate the necessity for construction of a duplicate circuit.

Each of these papers emphasizes the conservation of materials. This is particularly evident in the case of transformers and transformer loading—something which cannot be stressed too greatly. Mr. Cole has discussed this rather completely for line transformers and has referred to the ratio of demand on a circuit to the total installed transformer capacity. While it is true that this ratio is not a proper measure of loading of individual transformers, nevertheless, it is a figure which is readily available and, if used with discretion and judgment, may reveal a general condition of excess or possibly lack of line-transformer capacity. Obviously, if this ratio is 40 or 50 per cent on one primary circuit and 100 per cent or more on a similar circuit, an investigation is warranted, although such differences may be expected and justified on circuits serving loads of dissimilar characteristics.

Many companies have thoroughly studied the overload abilities of substation transformers in the light of load curves and have already taken advantage of increased loading of these power transformers, actually finding that loads in excess of name-plate ratings were not overloads at all. Excellent data are available regarding permissible loadings of power transformers, and a study of particular transformers may indicate that substantial overloads for short periods may result in no sacrifice in useful life or perhaps a small sacrifice which may be justified in the light of present conditions. In such analyses, proper allowances should be made for ambient temperatures at time of peak, possible low temperature rise of the transformer at full load as determined from original test data, possibilities of different types of forced cooling for short periods, and other factors brought out in these papers.

Mr. Cole has stressed the matter of secondary banking, and Mr. Gaty has called attention to the possibilities of secondary networks. Underground secondary networks have received much attention in the past few years, and their possibilities must be now generally appreciated. Much can be accomplished with overhead secondary networks by occasionally banking transformer secondaries in the less populated urban areas to take care of spotty poor voltage conditions. There should be many opportunities where extending three-wire secondaries "around the corner" or installing a "couple of spans" to connect two small transformer areas may obviate the necessity of changing transformer sizes, establishing new secondary districts, or changing secondary conductor sizes. The results of such practice may be surprising. It would seem that much greater consideration should be given to a limited use of such "baby networks," particularly in view of the excellent service record of low-voltage secondaries, which, when properly installed, cause little or no trouble.

None of the papers has referred to the increased capacity of a distribution system which results by changing from delta to wye operation. The entire history of overhead distribution is intimately associated with the changing of distribution systems from 2,400 volts, three-wire delta, to 4,160 volts, four-wire wye, with a common primary and secondary neutral. Similarly, in recent years some 4,800-volt delta systems have been changed to 8,320 volts wye, and it has been common practice to change 6,900- and 7,200-volt systems for wye operation. In the case of transmission lines, additional capacity has been secured by changing 24,000-volt transmission to 41,600 volts, and so forth. In many cases, studies have been made to determine whether some of these changes could be justified economically, and, if adverse decisions regarding such changes have been made, perhaps they should be reviewed in the light of material conservation. Certainly the capacity of a distribution system can be increased substantially by such a practice without the addition of copper. In some cases, generating equipment, synchronous condensers, or large motors now in operation may make such a change more costly, but the possible reconnection of this equipment or the use of autotransformers may be feasible.

It would seem that in this series of papers there should be some reference to the service voltage furnished to residential customers. While two-thirds of the lamps sold in the country are rated 120 volts (indicating voltage levels based on 120 volts), many systems are still operated on a 115-volt base. Since many of the complaints about low voltage result from unsatisfactory operation of appliances, would it not be opportune in the case of so-called 115-volt systems to consider raising voltage levels to 120 volts? By so doing, an improvement of from three to five volts can be secured for all types of utilization equipment without appreciably increasing complaints of high voltage except possibly for lamps. The 115-volt lamps would, of course, burn out more rapidly, but lamps sold in the area would be changed to 120 volts rating, and the voltage levels could be raised gradually over a period of several months until the 120-volt level is reached. Therefore, it is believed that consideration should be given to this suggestion, especially in view of the recent utilization voltage recommendations by committees of the Edison Electric Institute.

Messrs. Poage and Reid have pointed out the desirability of power-customer co-operation to help reduce system peak loads. Power-customer co-operation should also be encouraged to the greatest degree in connection with the power supply to new plants and the facilities which a large power user should require in order to guarantee dependable service. Surely the transmission, distribution, and substation design engineers should be able to give these customers the benefit of much valuable advice in planning their substation facilities, to the end that much material and equipment can be conserved by simplified layouts, with due consideration being given to continuity of service. This co-operation might extend to helping the customer determine his own distribution voltage, type of switching equipment, and outdoor substation facilities with special consideration given to the kind of transformers used, their proper taps, the necessity for spare units, duplicate busses, duplicate metering, and so forth.

The use of automatically operated airbreak switches to take the place of high-voltage oil circuit breakers as pointed out by Mr. St. Clair is worthy of much study by many companies. It is surprising how much can be done by properly planned installations of this type at enormous savings in materials used, cost of equipment, and time required for construction.

M. M. Samuels (Rural Electrification Administration, St. Louis, Mo.): The worst feature of this symposium is the fact that it did not take place three years ago or even five years ago, but I suppose that if it were presented then, the authors would have been called warmongers or even worse names, and possibly would have lost their jobs.

The symposium is certainly timely. The industry is struggling to keep up with production of additional generating capacity, even though some ostriches still claim that there is no shortage. There was no shortage of steel or copper either. Everything was lovely.

But very little has been said about a shortage of distribution capacity. These papers, indeed, present a very pessimistic picture, but in the last few months we have become accustomed and acclimated to pessimistic pictures. We got so that we can take it, and let us hope that we can take this one too.

The seriousness of the situation cannot be overemphasized. Reports from cities in which there is an enormous war activity indicate that distribution systems find it difficult to carry the existing loads, to say nothing of carrying additional loads, in existing industrial war plants and for war plants yet to be built.

Stretching will not solve the great problem of distribution capacity, particularly when the peak periods are no longer just 30 minutes or one hour but become four or five hours. We are in for a long war. Stretching may solve the problem for a very short time but not for a long time. When we get the additional generating capacity, the new powerhouses may be all dressed up with no place to go, because there will be very inadequate distribution. The problem must be considered as a part of our great war problem in general. No individual can decide or should take it upon himself to decide how important his activity is in the great problem of the war. This can only be decided by those who are responsible for running the war. If additional capacity is required to follow through with the government program of production, those in charge of running the war will have to see to it that this capacity is made available, and they will see to it.

Two years ago, before the Engineers Club at Raleigh, North Carolina (November 13, 1939) and again before the Florida Engineering Society (April 12, 1940), I spoke of "Power for National Defense" and had to be careful not to use the word "war."

I called attention to the need of preparing for this emergency. At that time many things could have been done by every utility that would have made the present problem easier. But there is no use of even talking about spilt milk. They just were not done, and it is never too late to start.

Mention is made in some of the papers of mobile substations as high as 15,000 kva. As an indication of how far behind we are in this respect from the viewpoint of a world war, I merely want to mention that in the London magazine, *The Engineer*, October 14, 1938, there is a photograph of a German portable substation of 100,000 kva, 220,000-volt to 10,000-volt, with all kinds of taps, and it was reported that the Germans had quite a few such substations. This is big-time power for war purposes, and we have to begin to think in terms of big-time power for war purposes.

Another item, a very important item, which is overlooked in all these discussions, and to which I called attention in North Carolina as well as in Florida, is the fact that in time of war, we must plan what we are going to do in case part of a power system is destroyed, either by enemies or by crackpots from within.

Nothing is mentioned in the papers about how the power that is left will be scheduled to the most important places, and whether or not the circuits are so arranged that they can be so scheduled. How will it be possible to bring whatever power is left to such places as hospitals, cold-storage plants, police radio, traffic lights, water supply, and so forth?

In case of a network, separate sources of supply to unusually important loads are very difficult to provide. I suggested on many occasions that every city, particularly those that have network systems, should provide themselves with a number of small mobile powerhouses that can run through the streets like fire engines and then bring power immediately to hospitals, first-aid stations, bomb shelters, and other such places. Very little has been done along this line, and it is never too late to start. One of the reasons, probably, why utilities did not take up the idea is psychological. Most of the small units are Diesel-driven, and the Diesel to a utility man has always been like a red flag to a bull.

To show what these little mobiles can do, I will recite an incident that happened only about a week ago. The army was about to start construction on a $26,000,000 camp and could not get a power supply brought it on time. One of the rural electrical co-operatives, quite a distance away, had 2,100-kw mobiles in temporary operation, pending completion of an incoming power circuit. The army asked for the mobiles to be able to start construction. Everybody worked that night to compete the circuit. In addition, some complicated legal papers had to be made out. In spite of that, the mobiles were on their way next day and got to the army site ahead of time. I have been told that they were pulled on the highway at 50 miles an hour. You all know, of course, that the Germans and the English have hundreds of small mobiles. We, too, should have had hundreds of them by this time.

Mention is made in one of the papers of load-curve control. This, too, is a very important item that has received insufficient attention. According to the paper, the particular company has been able to secure voluntary co-operation of the customers to reduce the peak. It should be possible everywhere to secure such co-operation and reduce peaks, thus adding considerably to the capacity of the power system.

We hear so much lately about a kilowatt-hour shortage as against a kilowatt shortage. When you speak of water power, a kilowatt-hour shortage can only mean a shortage of water. We had that. If you speak of steam, a kilowatt-hour shortage may mean either a shortage of fuel or a low plant factor brought about by a high peak. By load-curve control, whether voluntary or by force, it should be possible to reduce peaks considerably, and when peaks are reduced, both generating capacity and distribution capacity can take care of more energy.

In conclusion, I may say that, based on the papers presented here today, immediate action should be taken by all utilities to correlate distribution to generation and transmission, correlate the whole system to the government war purposes, and co-operate fully with those who are responsible for running and for winning the war.

In time of war: "Our country, right or wrong." Always, why not: "Country before company"?

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): One of the main thoughts throughout the papers is to increase the loading on existing equipment in order to reduce sharply the amount of critical materials required for new installations. As indicated in my 1939 AIEE paper, "Load Ratings of Cable,"[1] we have been establishing since 1930 new and higher temperatures for emergency operation of various kinds of cable, in order to postpone installations of new lines. I have been actively interested in the matter of operation at high temperatures since about 18 years ago when I had something to do with the research on paper insulation at the Massachusetts Institute of Technology.

Nevertheless, I should like to call for caution and perspective on this whole matter of temperatures for transformers, contacts in circuit-interrupting equipment, cable, and so forth. Our planning from the standpoint of the war emergency has to be on a basis of several years, because our government is planning the national efforts on the basis of the war's lasting several years. A recent Gallup poll showed that half of our citizens thought the war would last more than two years.

Tests and operating experience indicate that if one wishes to have a life of say 50 years for a given piece of equipment, then the operating temperatures may be anything up to a certain value, depending on the equipment. I have indicated this point at *A* in Figure 1. The limit may be determined by physical and chemical deterioration of the insulation as in a transformer, or by ionization as in an impregnated-paper-insulated cable, or by sheath cracking in the manhole as for a lead-covered underground cable, or by mechanical troubles as in a large alternator.

With further increases in temperature, the life of the equipment decreases uniformly, perhaps on a logarithmic basis down to a point, *B*, as indicated in the figure. In specifications and standards for cables and transformers in this country during the past few years or so, the industry has established temperatures for emergency operation which correspond to temperatures approaching point *B*. These temperatures are usually 10 or 20 degrees centigrade above our previous limits for normal day-in-and-day-out operation. Research at the Massachusetts Institute of Technology for The Engineering Foundation–AIEE project on insulating oils and cable saturants has shown that with continuous exposure of oil-impregnated insulation to oxygen, charring of the insulation occurs in a matter of a couple of weeks at 105 or 115 degrees centigrade, which is the range of the upper emergency temperatures recently established for transformers. Other researches, including a current one by the Commonwealth Edison Company, have shown that with no oxygen present, the rapid loss of the mechanical integrity of the insulation begins to occur at 120 or 130 degrees centigrade.

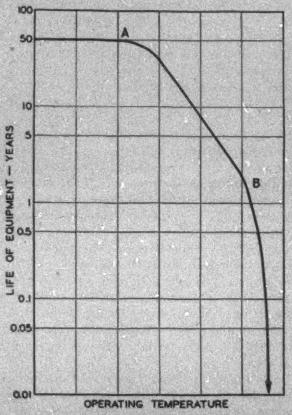


Figure 1. Temperature scale and shape of curve varies with equipment

If, during the war, equipment is normally operated at temperatures up to *A* and a little higher and equipment is operated at toward *B* during emergencies, in line with recently established limits, then we will increase our loading of existing equipment over past practice and cause some shortening of the life. However, the equipment will generally last without any particular trouble for the duration of the war. On the other hand, operation normally with temperatures toward *B* with occasional operation at temperatures above *B* during emergencies will greatly increase the likelihood of failures during the next few years. The new established temperatures for emergency operation are, incidentally, much above the limits in European specifications and standards.

Our operation in general should not be such that within a couple of years, and before the war is over, reliability of service would become very poor, and much equipment would be ruined and would need to be replaced when the country is short of critical materials and labor. Instead, installations should be made judiciously and consideration should be given to measures to avoid very excessive temperatures by methods such as indicated in today's papers and discussions.

I have a few special comments to make about Mr. Gaty's paper, "Underground Distribution Systems in Wartime":

Method of Supply, beginning of first paragraph: Regarding the relative advantages of a network system over a radial distribution system, our experience with the radial distribution four-kilovolt system has been, in general, quite satisfactory from the standpoint of service and economy of materials. Emergency temperature limits have been established for the underground cable and overhead wire and distribution transformers. Also, arrangements are made for emergency relief through emergency connections to take care of a load of a given feeder that has failed by having the load transferred to adjacent feeders.

Reliability of Supply, fourth paragraph: We have installed 66- and 132-kv transmission lines in the same conduit as lower-voltage cables without any bad consequences. The cables are each fireproofed in the manholes, and the relaying is fairly fast and has operated almost always as expected.

Reliability of Supply, fifth paragraph: The comments that the stock of cable and spare materials should be higher than normal are interesting, but the requirements of the War Production Board preference order may not permit putting this idea into effect in many cases.

Reliability of Supply, seventh paragraph: In connection with making quick repairs, one scheme that has been successful for many years in Chicago has been to connect the conductors of a joint and apply tape over the individual conductors, and omit soldering the joint sleeve to the cable, and omit filling the joint with compound. These open-air joints have been left to operate for several hours, until nighttime usually, when the joint construction has been completed with the line made dead again. Such operation has resulted in no troubles. Of course, where the water level tends to be high in the manhole, steps have to be taken to keep the manhole pumped dry for the several hours of the emergency. This general scheme has been used on joints rated at 12 kv and less.

The general ideas in the paper on the need and value of further work on limitations of insulations, conduit, and sheath are, to my mind, very much in order. It has been such work that has enabled industry in the past several years to establish emergency cable ratings that are 5, 10, or even 25 per cent above normal ratings.

Also, I want to emphasize point 4 in the author's summary on the need to "maintain an adequate inspection and maintenance program." With higher loading of equipment, the chances of trouble are greatly increased, but we may be able to give fairly reliable service if steps are taken to make relatively small expenditures for inspection and maintenance. This point, like many other points in this interesting paper, is one that many people agree on but do not always put into effect.

REFERENCE

1. LOAD RATINGS OF CABLE, Herman Halperin. AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–53.

M. S. Oldacre (Commonwealth Edison Company, Chicago, Ill.): Frequent reference is made in this group of papers to "defense industries" and "emergency conditions" or similar phrases. We should say "war industries, and so forth," so that it will be impressed on the minds of all of us at all times that we are faced with immediate problems and not with something we can do next week.

The authors have covered a wide field and necessarily have been able to devote only small space to each item. I should particularly like to comment on some of the transformer problems that we should face.

As pointed out in several places in the papers, most operating men try to avoid loading equipment so that any of the life will be consumed, and, in general, the nameplate rating is the limit. Under peacetime conditions this was justified as it has given some of the elasticity or slack in an electric distribution system that makes possible greater load capacity for war conditions.

The authors have made very little reference to how long the increased load is to be carried.

Shall equipment be loaded to the utmost, without regard to effect on the life of the apparatus, thus shooting all our ammunition in one shot, or shall the best available data be used as a guide to load the transformer to the limit that will still leave the transformer available for service as long as needed?

Most likely the latter method is the only one considered by most of us, and it is suggested that the recommendations in the American Standards Association standards for transformers be consulted as a guide for the average transformer. These data are based on the research of the larger transformer manufacturers and present, in more usable form, data partially covered in the references in the papers. The ASA recommendations are of necessity rather conservative, as they are intended to be applicable to almost any transformer, but I would not be surprised if many operating men voice loud objections.

For larger transformers and special conditions, study of the individual transformers is justified, and it will generally be found that more actual capacity is available than indicated by the name plate. In several instances such studies have delayed for two or three years additional installations costing a quarter to a half million dollars each. In still another instance a large transformer bank's capacity was increased one third to take care of a temporary condition that has now continued for 12 years.

Such studies develop along several paths, some of which are:

1. More extensive use of the transformers in their existing condition:

(*a*). This requires that the transformer will be loaded to the safe temperature limit for continuous operation, taking into account load cycle, ambient, and actual characteristics of the transformer.

(*b*). It requires further that for emergencies due to equipment failures, and so forth, the transformer will be loaded to at least the emergency limits of the ASA standards.

2. Providing additional cooling facilities of some of the types mentioned in the papers. In this case all additional facilities reasonably feasible should be added as one operation. Then loading for regular and emergency operation along the lines mentioned above should be employed.

New installations offer an additional problem that is well worth study and adoption where possible to save materials needed for war purposes.

In the larger installations load and load cycle is generally fairly well-known, and the transformers should be specified to provide conservative operation under normal conditions, but for emergency conditions the maximum temperature conditions that will still give a reasonable life should be specified.

This is best illustrated by an example: It is proposed for a specified load and load cycle that three transformers be installed, each capable in an emergency of carrying one half of the total maximum load. Normally all three will operate in parallel. An emergency may be of two days' duration to repair the underground cables that feed the transformers, or it may last for a month or two, if a transformer repair is necessary. It is proposed that for the emergency rating with forced air cooling, the temperature rise of the copper may be 75 degrees centigrade above the usual ambient of an average of 30, and a maximum of 40 degrees centigrade in 24 hours. This emergency rating would be about 60 per cent above the usual self-cooled rating that would be applied to the core and coils.

There is an indicated saving of materials of 35 to 40 per cent over a straight self-cooled transformer, and 15 to 20 per cent over the usual self- and forced air-cooled transformer. With other types of cooling, such as forced oil, circulation or spraying of water on the transformer might make further savings possible.

A good share of the saving will be in steel and copper—two strategic materials.

Philip Sporn: The striking fact brought out by all the discussions is not only that all utilities have been doing thinking on the problem, and that all have found particular ways and means of making existing distribution systems more effective, or ways and means of extending the capacity of distribution systems more economically than was felt desirable or even possible in peacetime, but that substantially they have been doing so for some time. One of the aims of the symposium was to bring all these ideas together and to place them as a complete unit before the distribution engineers of the power-supply systems. I believe the discussions show that it has accomplished that preliminary purpose. Once this has been done the final objective, that is, making existing distribution systems perform more work or making new systems perform necessary tasks more efficiently can, I am confident, be left to the skill, industry, and patriotism of those carrying on the detail task of designing, constructing, and operating the distribution systems of the country.

H. P. St. Clair: As we had hoped, the authors of the papers in this symposium have not been disappointed in the many valuable comments and suggestions which have been contributed by the discussers of these papers. To mention a very few, I should first like to endorse Mr. Halperin's timely warnings as to the possible dangers of going too far with the overloading of transformers to the extent that temperatures exceed or even closely approach the point B on the curve in Figure 1 of his discussion. While a considerable margin of safe overload capacity is undoubtedly available in many transformers, particularly on a short-time emergency basis, it is quite true that loss of life goes up rapidly, on a logarithmic scale with temperature, and also that temperature itself increases more rapidly as greater overloads are applied. It would indeed be false economy in the use of materials to allow failure of distribution or of transformer equipment to occur in the midst of our war effort. This only emphasizes the importance of a careful analysis of the overload capabilities of equipment so that the increased loading will be applied on a sane and sound basis.

Referring to the arrangement described by B. M. Jones, whereby a single-phase ground is intentionally applied by closing a disconnecting switch actuated by the differential relays of a transformer bank where high-voltage circuit breakers are not provided, I should like to say that the description of our typical "medium-capacity" transmission substation shown in Figure 4 failed to mention the fact that this station is, likewise, arranged for closing a disconnecting-switch ground when the differential relays operate. As a matter of fact, this scheme is employed on this and on one other 132-kv stepdown station on our system for the same reason as that described by Mr. Jones, namely, to insure clearing the high-voltage line in case of a transformer fault which would in itself not provide sufficient fault current to relay the high-voltage line.

The practice described by Mr. R. J. Hooke, whereby, in certain suitable instances, pairs of lines or cables can be paralleled on a single breaker and operated as a single circuit, is quite sound, and a proper addition to the various schemes mentioned in the paper. While it was not brought out as such, the arrangement shown in Figure 9 of the paper represents one such case where conditions were entirely favorable for this expedient. The practice has been followed in one other case, on the system with which the author is associated, and we are in full agreement that opportunities for such economies should not be overlooked.

Harold Cole: The discussion has brought out a number of valuable suggestions for conserving the use of material in overhead distribution systems which were not mentioned in my paper. Mr. Fuller and Mr. Sayles have pointed out the possibilities of converting 2,300-volt, three-wire delta systems to 4,000-volt, four-wire wye, and similar changes at higher-voltage levels, as a means of securing increased capacity with a minimum use of material. Certainly, expedience of this kind should be carefully studied, where the existing system connections are such as to make the change possible.

Mr. Sayles and Mr. McDowell have also advocated the "banking" of distribution transformers on the secondary side, as a means of conserving material. It has seemed to me that, in most cases where the practice has not been followed in the past, a considerable amount of new conductor material would have to be installed to do a proper job of "banking." It is, therefore, questionable whether a net saving of critical material would result. One of the principal advantages of "banking" is the reduction of voltage dips, and it has been pointed out that such minor inconveniences may have to be tolerated for the "duration." I hasten to add that I do not want to be taken as deprecating the practice of "secondary banking" as poor engineering in peacetimes. For many years the practice has been followed by The Detroit Edison Company, wherever the conditions were at all favorable, and its use justified from the point of view of economy as well as improvement to service.

Mr. Van Antwerp's comments on the need for encouraging ingenuity, organization, and enthusiasm, to carry out a program of getting the most use out of the material available, is certainly very much to the point. The practice cited by Mr. Fiedler of the use of two-wire services, which could later be readily converted to three-wire, is a good example of the type of ingenuity which accomplishes very worthwhile results. In this same connection, I understand that it is now possible to purchase meters which may be readily changed from three-wire to two-wire in place, at a very small increase in cost over the standard three-wire meter. This makes it possible to avoid the purchase of two-wire meters, which would later be difficult to use, because of the increasing requirement for three-wire services.

F. C. Poage and **M. W. Reid:** Mr. Samuels in attempting to cover the over-all situation, refers to shortages of facilities in almost universal terms. This is not a strictly accurate picture. There are also surpluses in certain locations, and there will be others in the future as many peacetime activities are curtailed. To the extent that facilities can be rearranged and reused, the apparent shortages will be relieved.

We have pointed out a few of the means for conserving essential materials and man power by the electric-power industry for "the duration" to the end that "the duration" may be shortened.

The discussion by Herman Halperin is particularly appropriate in this symposium, because of its clear exposition of the basic relation between operating temperature and useful life of organic insulating materials. It is the application of the principles which he has cited to the loading of equipment that we have urged in our paper.

It seems rather irrelevant, however, to speak of normal life of transformers and other equipment, when the life of the nation itself may be in jeopardy. If by loading equipment on a short-life basis, we can make more war materials and man power available now—not sometime in the future—to put into planes, ships, tanks, and guns, and thereby shorten the war by ever so little, the failures that may later ensue and the replacements to follow will be well worth their cost. The country needs the materials and the men *now*, and we must be guided at present more by the point of view of what we have to do now rather than by the strictly technical concept of what might otherwise be altogether prudent. The electric-power industry must and will, we are certain, make the necessary sacrifices to win the war and to win it quickly.

The Electric Strength of Nitrogen and Freon Under Pressure

Discussion and authors' closure of paper 42-33 by H. H. Skilling and W. C. Brenner, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 191–5.

G. C. Nonken (General Electric Company, Pittsfield, Mass.): The authors have expressed the breakdown voltage of the gases as tested between spheres in form of linear equations involving pressure and gap spacing. Their equations are of the form

$$V=KPS+C$$

when

V is the breakdown voltage
P is the absolute pressure (atmospheres)
S is the gap spacing in inches
K and C are constants for a particular gas

This equation is an expression of the well-known Paschen's law. Many research workers have found that their data deviate widely from Paschen's law at high pressures or at large gap spacings. Figures 1, 2, and 3 give the breakdown data of Freon, nitrogen, and air taken from the authors' and five other papers.[1–5] These tests are all made between spheres or uniform field gaps. The pressures range from atmospheric to 600 pounds gage pressure, and the spacings vary from 0.05 inch to 1 inch.

With few exceptions, these data lie on straight lines when the voltage gradient is plotted against the absolute pressure in logarithmic co-ordinates.

The general equation for these data is

$$\frac{V}{S}=CP^{b}$$

when

V is the breakdown voltage
S is the gap spacing in inches
P is the absolute pressure in pounds per square inch
C and b are constants. b represents the slope of the line and C determines its position vertically

The equation for the voltage breakdown of Freon is

$$\frac{V}{S}=24.4P^{0.805}$$

and is plotted in Figure 1. For these data, the constant C varies from 21.7 to 28 or over 20 per cent, while the constant b changes only from 0.79 to 0.805 or slightly less than two per cent. The variation in C indicates the differences due to the different investigators' test setups, electrodes, and so forth, while the constant b is affected very slightly by variations in test procedure but is a constant typical of the particular gas.

The authors' equation

$$V=183PS+4$$

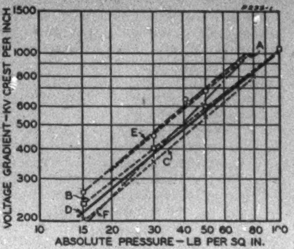


Figure 1. Breakdown voltage gradient of Freon

A—Skilling and Brenner—0.05-inch spacing
B—Trump, Safford, and Cloud—0.2-inch spacing
C—Nonken—0.39-inch spacing
D—Proposed equation $\frac{V}{S}=2.44P^{0.805}$
E—Skilling and Brenner equation $V=183PS+4$ ($S=0.05$ inch)
F—Skilling and Brenner equation $V=183PS+4$ ($S=1$ inch)

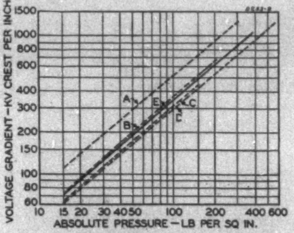


Figure 2. Breakdown voltage gradient of nitrogen

A—Skilling and Brenner	$S=0.05$ inch
B—Nonken	$S=0.39$ inch
C—Finkelmann	$S=0.76$ inch
D—Goldman and Wul	$S=0.079$ inch
E—Proposed equation	$\frac{V}{S}=7.6P^{0.835}$

for the breakdown of Freon is also plotted in Figure 1. This equation has different values of V/S for each spacing. The authors have chosen the correct constant for a 0.05 inch spacing, which is represented by curve E. The curve for a one-inch spacing according to the equation is shown by F. It is necessary to choose different constants for each gap spacing to make this equation fit the curve.

Figure 2 shows similar data for the breakdown of nitrogen. The equation suggested for this data is

$$\frac{V}{S}=7.6P^{0.835}$$

For this data, the constant C varies between 12.5 and 6.4, while the constant b varies from 0.8 to 0.835.

Figure 3 gives similar data for the breakdown of air. The equation suggested for this data is

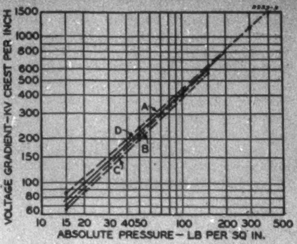


Figure 3. Breakdown voltage gradient of air

A—Howell	$S=0.25$ inch
B—Trump, Safford, and Cloud	$S=1$ inch
C—Finkelmann	$S=0.76$ inch
D—Proposed equation	$\frac{V}{S}=6.15\,P^{0.925}$

$$\frac{V}{S}=6.15P^{0.925}$$

For this data the constant C varies between 5.6 and 6.9, while the constant b varies between 0.875 and 0.935. Paschen's law is expressed by the above equation when $b=1$. Freon deviates the most from Paschen's law with an average value of $b=0.805$, while the values of b for nitrogen and air are 0.835 and 0.925 respectively. The breakdown characteristics of gaps which have corona previous to breakdown do not follow such a simple law as the one above. As the authors suggest, caution must be exercised in drawing any general conclusions or extrapolating any data in connection with the breakdown of sharp-edged gaps in high-pressure gas.

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2. HIGH-PRESSURE GAS AS A DIELECTRIC, **Gordon C. Nonken.** AIEE TRANSACTIONS, volume 60, 1941, December section, pages 1017–20.

3. DER ELEKTRISCHE DURCHSCHLAG VERSCHIEDENER GASE UNTER HOHEM DRUCK, **E. Finkelmann.** *Archiv für Elektrotechnik*, volume 31, 1937, pages 282–6.

4. BREAKDOWN OF COMPRESSED NITROGEN IN A NONUNIFORM ELECTRIC FIELD—II, **I. M. Goldman, B. M. Wul.** *Journal of Technical Physics* (USSR), volume 3, 1935, pages 16–27.

5. BREAKDOWN STUDIES IN COMPRESSED GASES, **A. H. Howell.** AIEE TRANSACTIONS, volume 58, 1939, May section, pages 193–204.

G. W. Dunlap (General Electric Company, Schenectady, N. Y.): In these days of shortages and priorities much thought and effort are being expended toward the utilization of substitute materials, but in the field of insulations the search for new materials has been going on for many years. More particularly and more recently much consideration has been given to the use of compressed gases in place of oil. This involves the comparison of the two mediums on the basis of not only dielectric strength but of weight, heat transfer, fire hazard, mechanical construction, maintenance, availability, and cost. These cannot be discussed at length here but must obviously be considered in detail for any given application. Since the paper under discussion concerns the dielectric characteristics of gases, these remarks will also be confined to those characteristics.

Gaseous insulation has been used satisfactorily in a number of special cases including cables, capacitors, electrostatic generators, potential transformers, and probably with the most spectacular results in the new million-volt portable industrial X-ray unit described in AIEE Transactions for December 1941. In addition, the increasing use of the air-blast principle in circuit breakers, while not ordinarily thought of in the same category with closed system insulations, is truly a great application of gaseous insulation. These uses amply justify the work which has been done and should encourage further investigation in this field where even now not too much is known. The following remarks, therefore, wherein they disagree with the authors should be taken as cautionary rather than discouraging in the application of gaseous insulation.

To take up the authors' conclusions in order, the relative strengths of nitrogen and air between smooth electrodes do not remain the same, even over the pressure range shown in Figure 1 if the length of gap is increased to the order of one centimeter. This is shown by Finkelmann[1] who found differences up to 20 per cent for one-centimeter gaps between flat plates 13.5 centimeters in diameter at 16 atmospheres, and up to 15 per cent at 10 atmospheres for gaps between 18 and 20 centimeters diameter concentric cylinders. Conclusion 2 is, of course, consistent with this.

Conclusion 3 regarding the relative strengths of Freon and nitrogen checks with the results of Charlton and Cooper (authors' reference 3) and Nonken,[2] and also with the experience of the writer which indicates that the 2.5 ratio holds out to spacings of at least four centimeters between 6.25-centimeter spheres. However, the formula $V = 183PS + 4.0$ does not necessarily hold for this same range as may be seen from Table I where some of the writer's data which are comparable with Nonken's are compared with values calculated from the formula.

Regarding mixtures of Freon and nitrogen (conclusion 4), the question might be raised as to the practicality of providing a fan for mixing the gases as the authors found necessary.

For the case of Freon between pointed or other corona-forming electrodes, irregularities of such magnitude exist that the data presented by the authors—data which do not include some of these irregularities—may give too optimistic a picture of the general situation. It is true that the strength of Freon may be four times that of nitrogen as stated by the authors, and, in fact, the ratio may be even greater under some conditions. For example, Nonken shows about six times for a six-centimeter standard rod gap at atmospheric pressure. However, under conditions of greater spacing and voltage than the range covered by the authors, Freon gives a "hump" in the pressure-voltage curve similar to that shown for air in Figure 7, except that the maximum occurs at a much lower pressure in the Freon. For this reason the strength of Freon may actually be less than that of nitrogen at some pressures, and the writer has taken data in which this was the case.

Table I. Breakdown of 6.25-Centimeter Sphere Gap in Freon

PS	Test Kv	Formula Kv	Per Cent Deviation From Formula Kv
		1 cm spacing	
0.39	90	76	+18
0.78	155	147	+ 5
1.58	225	291	−23
2.36	300	436	−31
		2 cm spacing	
0.78	130	147	−11
1.58	255	291	−12
3.15	350	580	−40
4.72	460	868	−47

In addition, and even more important in many applications, the high strength in the "hump" portion of the curve results in situations where the 60-cycle strength may be twice the impulse strength, and an impulse ratio comparable to that of transformer oil is rarely obtainable. This phenomenon has been verified by both Nonken and the writer. As the authors point out, this high 60-cycle strength is found with corona-forming gaps, but it would seem to be a case of more corona shielding in Freon due to decreased mobility of the space charge rather than an impeding of corona formation as indicated. A number of investigators[2,3] have measured the corona starting voltage in Freon and other gases and have found no discontinuities corresponding to the "hump" in the breakdown voltage curve.

As pointed out above, there have been several applications of gaseous insulation in which very satisfactory performance has been obtained. Experience shows, however, that it is necessary to study each application in detail with careful consideration of its particular requirements. If these requirements are such that conditions for optimum gas performance can be maintained, the use of gas may prove very advantageous. For such a case marked reductions in size and weight of equipment may be effected as was done with the million volt X-ray outfit.

References

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2. High-Pressure Gas as a Dielectric, **G. C. Nonken.** AIEE Transactions, volume 60, 1941, December section, pages 1017–20.

3. The Effect of Pressure on the Positive Point-to-Plane Discharge in N_2, O_2, CO_2, SO_2, SF_6, CCL_2F_2, A, He, and H_2, **H. C. Pollock, F. S. Cooper.** *Physical Review*, volume 56, July 15, 1939, page 170.

Henry M. Hobart (Consulting Engineer, Schenectady, N. Y.): Conclusion 6 of the authors' paper states that, for dielectric purposes, compressed Freon would be inferior to compressed nitrogen at compressions greater than 150 pounds per square inch (10.5 metric atmospheres). At that pressure and for a 50-mil spacing between one-inch-diameter spherical electrodes, the 60-cycle electric strength of Freon, estimated by the authors' formula, is 2,000 crest volts per mil. For that gap and spacing it is the writer's opinion that the 60-cycle electric strength of nitrogen will not exceed 1,600 crest volts per mil, no matter how great a compression is employed.

Corresponding to a compression of 10.5 metric atmospheres, the condensation temperature of Freon is 44 degrees centigrade. The writer would value an expression of opinion by the authors as to any difficulties foreseen by them in employing Freon in cases where (for the attainment of a particular purpose) a temperature considerably greater than 44 degrees centigrade is required. For example, at a temperature of 100 degrees centigrade a compression of 25 metric atmospheres would not condense Freon gas, and its 60-cycle electric strength for a 50-mil spacing between one-inch-diameter spherical electrodes almost certainly would be considerably more than 2,400 crest volts per mil (that is, more than 50 per cent greater than nitrogen's limiting strength of not over 1,600 crest volts per mil for that spacing).

The application which the writer has in mind, relates to a compressed-gas transformer in which the gas shall partly or completely condense after each passage through the active material (core and windings) of the transformer, each time returning to its starting point cooled by having transferred its heat by the vaporization of a liquid of appropriately low vaporization temperature. It is evident that for such a cycle the conditions are improved, and the size and cost of the transformer and heat exchanger are decreased, the higher the permitted temperature. If, in such a vaporization-cooled transformer, there can be employed a temperature very much above 100 degrees centigrade, the liquid to which the gas transfers its heat, can be water, and we have the advantage of its wide availability, its usually almost negligible cost, and its very great latent heat.

The writer would value the authors' opinion (based on their considerable familiarity with Freon), as to how high a temperature could be employed for the Freon gas without encountering any prohibitive chemical difficulties. Owing chiefly to their higher condensing temperatures for given compressions, and on account of their price and of their dielectric characteristics at high pressures, the writer considers SO_2 and CCl_4 to be attractive alternatives. Consequently he would also much appreciate the authors' comments anent these materials for the purposes here described. Such a design obviously involves various compromises. It is believed that we now have available a considerable variety of adequate heat-resisting materials for the turn and layer insulations and for their impregnating constituents.

Amongst other features which will be embodied in such transformers is the decreasing of the core loss by employing the "Maclaren effect." Since this feature requires a core temperature much in excess of desirable winding temperatures, the core will be separated from the windings by a "thermal screen" of asbestos or fiber glass or Mycalex or other suitable material. High-voltage compressed-gas power transformers embodying these various features will not require any high-voltage entrance bushing but will be supplied through a surge-attenuating underground transmission line comprising compressed-gas-insulated conductors enclosed in a welded-steel pipe line.

Thus we arrive at a system providing 60-cycle underground conductors for the long-distance high-voltage transmission of great amounts of power and for power pools. No time should be lost for seeking economical alternatives for our present outmoded overhead, high-voltage, power-transmission system with its aboveground, outdoor installations of oil-type power transformers and switchgear. If the project here briefly sketched could be promptly developed and employed, then the impending annual expenditure of many hundreds of millions of dollars for the urgently required increase in the nation's electricity supply need not perpetuate the decades old constructions with their serious susceptibility to interruptions from lightning, from storms of ice and sleet and wind, from enemy depredations in wartime, and from interference with aircraft traffic and with communications systems. The high-voltage transformers no longer will require expensive and none too dependable inlet bushings of awkwardly great height, nor internal shielding for surge protection, nor will they contain inflammable and sludging oil which imposes a very low temperature limit. Their compressed-gas successors will be lighter, cheaper, and in all respects more dependable. The consequent conservation of the nation's oil supply is also a factor not to be overlooked.

Our present overhead tower systems and our oil transformers are carryovers from the historic Frankfurt-Lauffen long-distance transmission installation built just 50 years ago. To meet the vastly more severe modern requirements the fundamentals of this pioneer line have been retained to a great extent but revamped to provide for more power and higher voltages. For their present tolerable adequacy thanks are due to the skill and ingenuity of many able engineers. The result, however, has been attained at a cost which seriously handicaps the desired far wider use of electricity and with a disappointing degree of dependability.

The writer recalls the interesting article by one of the authors of the present paper which was published in *Electrical World* a few years ago. The article was devoted to the description and advocacy of a compressed-gas underground system in which the high-voltage transmission conductors were pipe-enclosed. It would be of much interest to learn from Professor Skilling to what further extent he has gone with the development of that project.

The authors have done well in especially emphasizing the extremely important characteristic of Freon in virtue of which its electric strength is not so greatly impaired by increasing nonuniformity of the field as is the electric strength of nitrogen. Thus in analyzing the results published in Mr. Nonken's recent AIEE paper entitled "High-Pressure Gas as a Dielectric," the writer finds that with a pressure of nine metric atmospheres and a spacing of 800 mils, Freon's electric strength in the rod-gap test decreased to 40 per cent of its strength in the sphere-gap test, whereas the electric strength of nitrogen under these same conditions decreased down to only 30 per cent of its sphere-gap strength. The authors' tests with pointed electrodes as shown in their Figure 7, disclose the same trend, but much intensified, as they apply to a greater extreme of nonuniformity of field than corresponds to the rod gap.

This paper is certain to be of much value to those working on application problems involving the use of compressed gas as a dielectric. Already it has proved very useful indeed to the present writer.

Wm. C. Brenner and **H. H. Skilling:** The principal purpose of this paper is to present experimental results on the electric strength of Freon gas and of mixtures of Freon and nitrogen, giving information covering a region of pressure and electrode spacing for which adequate data have not been published. As a convenience in estimating the sparking voltage under the conditions of the experimental work, an equation for sparking voltage is given.

Much of the discussion is related to the applicability of this equation to other ranges of pressure and spacing. Mr. Gordon Nonken proposes an alternative equation in his discussion, of the form $V=CP^bS$. He suggests the application of this form to sparking in air and nitrogen as well as in Freon. He says very truly that many investigators have found that their data deviate widely from Paschen's law at high pressure and large spacing. This was emphasized in a previous paper by the authors on "The Electric Strength of Air at High Pressures—II" (reference 2 of the present paper). In that earlier paper a modification of the familiar form of Paschen's law was given to apply to the authors' results in air. Paschen's law, applicable at pressures up to atmospheric, and substantiated by physicists for over half a century, is of the form

$$V=75.4PS+1.7 \text{ kilovolts} \qquad (1)$$

P is in atmospheres, S in inches. The modification to fit the authors' data at pressures up to twenty atmospheres was

$$V=\frac{76.0PS}{1+cP}+1.7 \text{ kilovolts} \qquad (2)$$

This equation 2 has the advantage of reducing to the accepted form of Paschen's law at low pressure (c is a constant much smaller than unity). Mr. Nonken's formula does not share this advantage; in particular, the constant term is absent from his exponential form which approaches zero sparking voltage at low pressure. Paschen's law, to be sure, fails to apply if the pressure-spacing product is below 0.05 inch-atmospheres, but an exponential formula will deviate from observed data considerably sooner.

Equation 2 has the further advantage of taking into account the length of spark gap. It will be seen from Figure 3 of Mr. Nonken's discussion that the voltage gradient is *not* independent of the gap length; the authors' equation 2, above, takes this into account, and results computed from this equation may be plotted in Nonken's Figure 3 as a family of lines, one for each spacing. This appears somewhat more satisfactory for representing the family of experimental curves than does a formula giving a single line. Equation 2 gives excellent agreement with Howell's results, and good agreement with Trump's, if compared with the curves published by those authors. The equation also agrees with the present authors' measurements in air (reference 2) which could be plotted in Mr. Nonken's Figure 3 as a curve lying above all those shown but approaching them at very high pressure.

In devising a formula to apply to experimental results in Freon, the authors naturally sought a form similar to Paschen's law. It seemed desirable not only for its simplicity and familiarity, but also because it is a form so well-established in air and other gases. Thus the equation suggested is:

$$V=183PS+4.0 \text{ kilovolts} \qquad (3)$$

It must again be emphasized that this equation does not apply near the pressure at which Freon condenses to a liquid and therefore should not be used above about four atmospheres.

Whether this form or another is used is a matter of choice at present. There is not yet enough data to permit a decision, although it seems likely that an equation, to take into account all the peculiarities of Freon, would necessarily be a fairly complicated one. The authors' equation 3 gives reasonable agreement with their own results. It gives even better agreement with the results published by Trump, Safford, and Cloud in their Figure 4 (of reference 9) at pressures below 60 pounds per square inch absolute. No attempt was made to derive a formula for a wide range of pressure and spacing, for it is our opinion that more information is needed before that can profitably be done.

Dr. Dunlap presents new and interesting data that substantiate this opinion; yet it is remarkable that the authors' equation 3 turns out to be as good as it is under conditions of board extrapolation. With extrapolation of the pressure-spacing product to five times that for which the formula was designed, the deviation between the experimental results and the formula is given by Dunlap as only about ten per cent.

Dr. Dunlap shows that in other cases the deviation is as much as 30 or 40 per cent, but since his data were obtained at pressures practically as great as the vapor pressure of Freon at room temperature, and quite beyond the range of equation 3, this is not surprising. It seems best that experimental sparking values in this pressure range be used directly, for generalization in any kind of formula would probably be unsafe.

Regarding the mixing of Freon and nitrogen, the authors suggest that in commercial applications of gaseous insulation it would be more convenient to mix the gases before injecting them into the equipment to be protected. When once mixed they will, of course, stay mixed, and there is no need for a fan within the vital parts of a cable or a million-volt X-ray outfit.

Mr. Hobart's interesting discussion brings to attention several points that might be clarified.

1. The conclusion 6 to which Mr. Hobart refers should have been qualified for the sake of clarity, for it was intended to show that Freon at room temperature was inferior to nitrogen at 300 pounds per square inch for the various stated reasons, and it was meant to convey the idea that where structural or mechanical limitations would not allow pressures above 150 pounds per square inch, Freon (at room temperature) should be carefully considered.

2. The equation $V=183PS+4$ for the breakdown kilovolts (V) for sphere gaps (where S is in inches and P in atmospheres) was determined at temperatures ranging from 55 to 73 degrees Fahrenheit (considered to be constant at the time of measurement) and for pressures all below the vapor pressure of Freon at room temperature.

To extrapolate the curve for higher pressure regions, one should take into account the temperature effect realizing that the density of the Freon is the important factor. For constant temperature the density was adequately represented by pressure, but to be more complete the form of the breakdown equation should be of the type

$$V = \alpha \frac{P}{T} s + \beta$$

Again it should be mentioned that the original equation was not checked at the vapor pressure, and unless it is intended to use Freon at elevated temperatures where the gas has some degree of superheat, it would be well to check the equation for several points at the vapor pressure, lest unhappily saturation of the gas may be accompanied by some sort of discontinuity.

As to the stability of Freon and the other mentioned gases in Mr. Hobart's discussion, it is well to consult the manufacturer as to the thermal decomposition of the various gases. As to the effects of electrical disintegration, none need be considered unless there is an electrical discharge taking place, and the degree and seriousness of this should be determined by the particular application. For smooth surfaces with no or only occasional arcing, decomposition may be disregarded, but for applications where corona may possibly exist, the seriousness of the decomposition is to be determined by the effects of corrosion and the requirements of adding or changing the particular gas in question being used for its high insulating properties.

Referring to the proposed Freon-cooled transformer feeding into a Freon-filled cable, one thing must be remembered, and that is, that if high pressures are to be utilized, the entire interconnected system must be maintained at a temperature sufficiently high to prevent the Freon's liquefying, thereby reducing its high insulating strength.

It seems that Freon would best serve at ordinary temperatures and pressures below the vapor pressure where the entire system could be maintained at a constant pressure; otherwise, there would arise the above mentioned problem of maintaining elevated temperatures for the entire system, or the need for gas tight bushings between the various elements of the system. However, for special cases the use of high-pressure Freon may prove quite valuable and should bear further investigation.

Saturated Synchronous Machines Under Transient Conditions in the Pole Axis

Discussion and author's closure of paper 42-43 by Reinhold Rüdenberg, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 297–306.

S. B. Crary (General Electric Company, Schenectady, N. Y.): This paper presents an interesting graphical analysis of the effect of saturation on synchronous-machine performance. Any analysis of the influence of saturation must necessarily be approximate. When such approximations are necessary, the value of the analysis is increased when it is used with a full knowledge of the assumptions made. Dr. Rudenberg's analysis is necessarily limited to the effect of saturation in the main flux paths rather than in the leakage paths and is more nearly correct when the major part of the saturation is in the pole body. The analysis, therefore, can be used for transient disturbances, which are for periods of time in which the relatively slow field transient is important, such as in the calculation of alternator swing curves.

The method described in the paper is similar to that given by Mr. F. R. Longley in "Calculation of Alternator Swing Curves—Step-by-Step Method" (AIEE TRANSACTIONS, volume 49, 1930, page 1129), except that Rüdenberg suggests taking into account the additional effect of saturation by the addition of an equivalent field magnetomotive force acting along the direct axis. This method of taking saturation into account has been used in the calculation of swing curves with the network analyzer, except that the pole saturation was corrected, not only as a function of the direct-axis pole flux, but also as a function of the air-gap flux, in order more properly to take into account the effect of stator saturation. In terms of per-unit quantities and the nomenclature which has been established in this country, Rüdenberg's equation for calculation of the effect of saturation when a machine is connected to an external reactance would take the form of

$$T_{do} p e_{\psi} = e_I - \frac{x_d + x_e}{{}_d'x + x_e} e_{\psi} - e_s$$

where

T_{do} = open-circuit field time constant
e_{ψ} = voltage behind transient reactance
e_I = voltage corresponding to exciter voltage
e_s = voltage corresponding to saturation magnetomotive force

For swing-curve calculations the vector diagram is of the form shown in Figure 1. e_s can be determined as a function of the voltage back of Potier reactance, which may be less than the transient reactance, as well as of the direct-axis pole flux. Although this method, as well as Rüdenberg's method, is approximate, it does give an answer which more accurately describes the machine performance than if saturation were neglected entirely. The point of interest is that by using the main-field winding flux or voltage behind transient reactance, the calculations can be carried out, using the open-circuit time constant in the equation for the rate of change of field-flux linkages without concern as to the impedance of the external circuit. This was previously pointed out by Longley for the case of no saturation.

The method described by Rüdenberg is approximate in that:

1. It neglects the effect of voltage produced by the rate of change of stator-flux linkages which, as he mentions, produces a d-c current or natural frequency oscillation.

2. It assumes rotor flux as only a function of the open-circuit saturation curve and therefore neglects

(*a*) Effect of saturation of the leakage paths due to stator currents.

(*b*) Effect of field-current load saturation. Actual rotor saturation is a function of both field excitation and direct-axis rotor air-gap flux, as shown in reference 13.

(*c*) Effect of change in leakage-path saturation due to redistribution of field-leakage flux.

The errors due to these assumptions are particularly appreciable under short-circuit conditions, and it becomes necessary, in any analytical method which attempts to approach actual conditions, to include properly these effects. It has been previously shown that saturation of the leakage paths is the principal cause for low equivalent short-circuit reactance. It is not sufficient to assume, as Rudenberg has done, that the subtransient current can be determined at the first instant by including only the stator leakage reactance and neglecting the effect of amortisseur or rotor leakage reactance. Actually the stator leakage reactance is reduced, particularly in the case of turbine generators. Then the problem becomes one of determining the amount by which both the stator and rotor leakage reactances are reduced.

As will be readily recognized, Rüdenberg's method assumes that the transient reactance and Potier reactance are equal to the sum of the stator- and rotor-field leakage reactances. This is not the case. It is well known that Potier reactance varies, depending upon the amount of load saturation, and even at normal operating voltages is usually less than transient reactance ("Armature Leakage Reactance of Synchronous Machines" by L. A. March and S. B. Crary, AIEE TRANSACTIONS, volume 54, 1935, April section, pages 378–381).

R. Rüdenberg: The discussion by Mr. Crary indicates that he is still wholeheartedly on the ground of the classical linear theory, which has had its great merits in clarifying the principles. Significantly, he begins with the assertion that "any analysis of the influence of saturation must necessarily be approximate" and with similar statements of axiomatic character.

Fortunately, though, this is not the fact. The saturation in synchronous machines can be taken into account by methods having the same rigor as the classical methods have for the linear case. It is only necessary to develop a transient analysis which can be applied graphically rather than analytically,

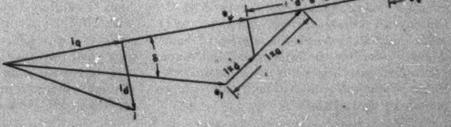

Figure 1

since for the most part the saturation functions of machines are given graphically.

If Mr. Crary will go over the paper again, free from the bias of a nomenclature of parameters developed solely for nonsaturated machines and not applicable therefore to the present case, I think he will be surprised to see how simply the rigorous consideration of the saturation under transient conditions actually works out.

It is by no means necessary to develop first a nonsaturated transient theory as most authors, including those referred to by Mr. Crary, have done for fear of a too complicated saturated analysis, and then try to correct such a linear theory by "additional effects" of saturation. It is the object of my paper to consider saturation from the very beginning as a fundamental element and to show that a rigorous and yet a highly practicable nonlinear analysis can be developed without the crutch of subsequent corrections.

Mr. Crary deduces as an analogue to my main relation (equation 14) a similar but not identical equation expressed in terms of his preferred nomenclature. However, his is not a relation of equivalent value, since it contains parameters which are defined for nonsaturated conditions only and actually are not constant for saturated machines. Thus, although the analysis sketched by Mr. Crary may be, according to his own words, an approximate one, due to his introducing saturation a posteriori, the method of my paper is definitely a rigorous one in considering saturation a priori. All the many omissions, assumptions, and approximations which Mr. Crary ascribes to the new method are by no means actually contained in it but are merely imaginary, caused by his viewing the present problem from an unsuitable angle.

This may be verified by two examples among many others. Mr. Crary asserts in his second and third paragraphs from the end that I have neglected saturation of the leakage paths due to stator currents. However, he seems to have overlooked paragraph 9 of section 4 of my paper, with equation 29 and Figure 16, where I show analytically and graphically the manner in which saturation of stator and rotor leakage paths enters this analysis and can be considered rigorously from the very beginning. Also, the last sentence of my paper, before the summary, generally treating nonlinearity with current, seems to have been overlooked by Mr. Crary in writing his discussion.

In his last paragraph, Mr. Crary asserts that my method would assume the "transient reactance" and "Potier reactance" as equal to the sum of the stator and rotor leakage reactances. This is contrary to my statements. Actually, I have proved in paragraph 7 of section 5, by means of Figure 19, that "transient reactance," a classical notation which I do not use elsewhere in my paper, with saturated machines always appears materially greater than the sum of stator and rotor leakage reactances. "Potier reactance," on the other hand, is, for good reasons, not used nor mentioned in my paper.

And so it is with all the other objections of Mr. Crary. It seems to me that by the simplicity of the new method, which considers saturation as a primary effect, Mr. Crary is misled in believing that this method would not cover the many secondary effects which apparently he has in mind. Simplicity, however, is not of itself a preventive of accuracy.

Formulas for the Magnetic-Field Strength Near a Cylindrical Coil

Discussion and author's closure of paper 42-27 by H. B. Dwight, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 327–33.

A. U. Welch (General Electric Company, Pittsfield, Mass.): The formulas in this paper form a very complete solution of the problem of computing magnetic fields within and near a cylindrical coil. Where great accuracy is not required, the desired information could be given much more conveniently by curves. The writer had plotted such curves a number of years ago and has found them very convenient. They are, therefore offered here in the hope that they may prove helpful to others.

These curves are plotted from formulas derived by the writer by integrating and differentiating well-known formulas for mutual inductance and magnetic potential. The formulas used were not so complete as those in the paper, and it was necessary to resort to numerical integration methods with formulas for circles similar to equations 12 and 13 in the paper to bridge the gaps between the limits of convergence of the different formulas. The curves used are as follows:

Radial Flux. It will be seen by differentiating and integrating that the radial

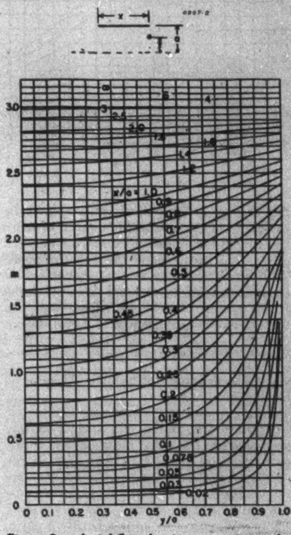

Figure 2. Axial flux density at a point in the end plane of a cylindrical current sheet, point having smaller radius than the current sheet

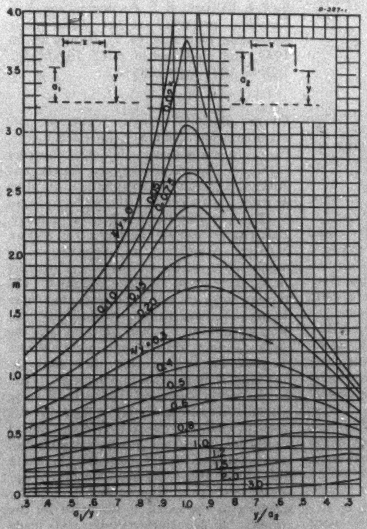

Figure 1. Curves of mutual inductance between coaxial circle and ring

Used for computing radial flux density

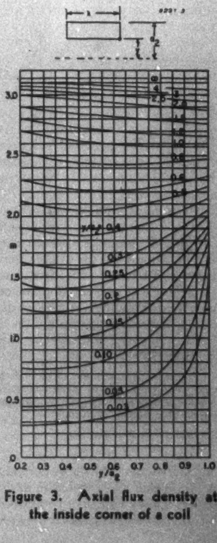

Figure 3. Axial flux density at the inside corner of a coil

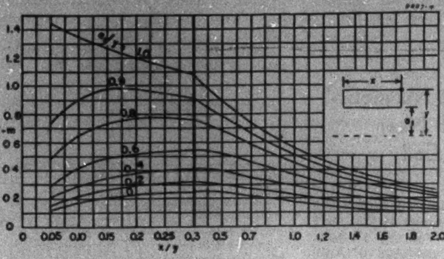

Figure 4. Axial flux density at the outside corner of a coil

flux density at any point is a function of the difference of the mutual inductance between the ring which is the boundary of one end of the coil and a circle through the point, subtracted from the mutual inductance of the other end of the coil and the same circle. If the quantity m be so defined that the mutual inductance is equal to $4\pi ym$, then

$$H_r = 0.2nI(m_1 - m_2)$$

where the definition of terms is the same as in the paper.

Figure 1 and the above formula permit computation of radial flux density at any point having the same radius as the inside or outside of the coil. (Note that in the sketches on the curves the dotted lines are the center lines of the coils, and only one side of the coils is shown.) By subdividing a coil into parts or by adding fictitious parts, if the point is beyond the inner or outer radius of the coil, and weighting the contribution of each part according to its radial thickness (number of turns), the field can be computed at any point within or near the coil.

The radial flux caused by a cylindrical current sheet of zero thickness becomes a function of the difference of mutual inductance of circles. Since complete tables of mutual inductance of circles are available,[1] no curve is necessary for this case.

Axial Flux. Figures 2, 3, and 4 give axial flux density at points in the end plane of cylindrical current sheet and at the inside or outside corner of a thick coil. Axial flux density

$$H_x = 0.2nIm$$

It should be noted that the direction of axial flux is opposite on the inside and outside corners of coils.

By superposition of parts of coils the flux density at any point within or near a coil can be calculated as outlined above.

The curves are unsuitable for computing fields at a large distance from the coil, since the result will be the difference between two nearly equal quantities. Formulas are available which converge rapidly for this case.[2]

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1. TABLES FOR CALCULATION OF MUTUAL INDUCTANCE OF CIRCUITS WITH CIRCULAR SYMMETRY ABOUT A COMMON AXIS, F. W. Grover. Scientific paper 498, Bureau of Standards.

2. Reference 2 of the paper.

H. B. Dwight: The curves presented by Mr. A. U. Welch are a welcome addition to my paper. They can save computation by the formulas, which is a rather long piece of work in some cases. His discussion indicates that this general problem has practical applications in engineering. For instance, the mechanical force on a corner conductor of a current-limiting reactor can be computed as the product of the current in that conductor and the magnetic field strength at the corner of the coil.

Example II of the paper is checked very quickly by Figure 2 of the discussion. At $y/a = 0.6$ and $x/a = 0.4$, m as read from the curves is 1.45. Then

$$H_x = \frac{2nI}{10} \times 1.45 = 2.90\,\frac{nI}{10}$$

Impulse and 60-Cycle Characteristics of Driven Grounds—II

Discussion and authors' closure of paper 42-22 by P. L. Bellaschi, R. E. Armington, and A. E. Snowden, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, pages 349–63.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In a discussion of a contemporary paper by Messrs. Robertson, Lewis, and Foust, "Lightning Investigation at High Altitudes in Colorado," the writer referred to effects of earth resistivity and geology. This relates also to the paper by Messrs. Bellaschi, Armington, and Snowden. The conductivity of the soil depends principally on the presence of moisture and salts. The ancient rocks are low in porosity, moreover during their long lifetime, soluble salts have been washed out. Where they lie near the earth's surface, resistivity is high. Where the ground resistance is low, there are moisture and soluble salts making electrolyte. In the writer's opinion, the reductions in ground resistance observed at high currents in soil of relatively low resistivity are caused largely by the change in resistivity of electrolyte produced at high currents, whereas in soils of high resistivity the reduction in apparent resistance is produced largely by sparking in the earth as discussed by the authors. In fact, during similar tests made at Trafford in 1929, sparking was actually observed visually on high-resistance grounds when surged with high currents. Those tests were not as exhaustive as the more recent ones described in the Bellaschi, Armington, and Snowden paper, but the results were of similar character. The publication of the early Trafford tests,[1] contained a caution which may bear repetition. The reduction in resistance of what is termed a high-resistance ground achieved at high-surge currents does not necessarily make it a good ground, since the discharge is accompanied by high voltage. The resistance as determined with low-voltage measurements is a good guide to the efficiency of grounds.

In regions of great geological age, the behavior of grounds is of particular interest, since they are inherently high in resistance. Data on this would be of value.

REFERENCE

1. *Railway Signaling*, April 1929, pages 145 and 146, and *Electrical World*, December 9, 1933, pages 751 and 752.

C. Francis Harding* (Purdue University, Lafayette, Ind.): This discussion emphasizes the importance of the use of laboratory tests and models in predicting probable characteristics of grounds in practice. All will recognize the desirability of having the tests made upon actual line installations in so far as possible. However, this is particularly difficult, expensive, and slow of solution in the case of surge potentials. Even in the case of the additional protection against lightning of 4,000-volt distribution lines by means of the interconnection of secondary neutral to lightning-arrester grounds, several years ago, before this Institute, Mr. Roper stated the fact that the laboratory investigation had solved the problem in many years less time and at much less expense than would have been possible by awaiting actual lightning strokes.

Professor J. H. Karr of Purdue University, among others, established a satisfactory close correlation between 60-cycle ground impedances in small volumes of various soils in the laboratory and in actual installations. With the apparently wide variations of such impedances with the shape and depth of grounding electrodes, and with the temperature, moisture content, density, and chemical analysis of various soils, it is obvious that laboratory tests should provide an early solution to such a problem. The authors of this paper have been fortunate in having several types of soil within reach of a surge generator. It is not evident from the paper, however, that ground electrode shapes other than rods of limited diameter were used.

Mr. J. R. Eaton, of the staff of the new high-voltage laboratory of Purdue University, in co-operation with Messrs. Lewis and Foust, is engaged in an attempt to secure further data involving the previously mentioned variables in volumes of various soils in the laboratory using the wide variety of grounding electrodes, sometimes adopted in wooden-pole transmission-line construction. It is hoped that such tests will determine the conditions which contribute most to low impedance offered to surges. Later, by means of a portable surge generator, it is hoped to secure correlations between such laboratory findings and actual installations. Such a procedure should be more rapid and much less expensive than a statistical analysis of actual lightning conditions. Both may be necessary for a complete solution of this important problem.

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*Deceased April 13, 1942.

Hamilton Treadway (United States Department of Agriculture, Washington, D. C.): This paper is a valuable extension of the author's previous work on this subject.

There is one point which may give rise to some controversy. The author proposes that the decrease in the resistance of driven grounds with increasing impulse current is due to an increase in effective radius and length due to the increase in voltage gradient. It cannot be doubted that the voltage gradient on the rod results in an increase in both the effective length and radius of the rod. However, it is doubtful whether this is the sole reason for the decrease in resistance.

1. This conclusion assumes that the shell of earth around the ground rod, after the breakdown occurs at the critical gradient, has the same conductivity as the rod, and that the contact resistance of this shell of earth to the surrounding soil is the same as that of the ground rod to the soil. This assumption is obviously incorrect but for all practical purposes may be justified. This point might be clarified by conducting tests on various size rods using values of crest current to give equal voltage gradients on each rod.

2. This conclusion ignores the relation between the leakage resistance of the soil, the contact resistance between the rod and soil, and the capacitance of the rod. The ground rod and lead circuit is essentially an imperfect capacitance in series with a resistance and inductance. The contact resistance is in parallel with the capacitance of the rod. Under transient conditions as the circuit approaches series resonance, the contact resistance is effectively shunted out, and the resistance indicated is essentially the leakage resistance of the earth itself.

The relation between the constants of the ground circuit is an important one. As pointed out by Mr. Hagenguth in his discussion of the author's first paper on this subject, the impulse impedance will rise rapidly during the first few tenths of a microsecond and in all probability will greatly exceed the 60-cycle value. The impulse impedance-time curve for the parallel combination of grounds *F*, *G*, *H*, and *I* in Figure 13 of the author's paper substantiates the existence of an initial impulse impedance exceeding the 60-cycle value. This initial rise in the impulse impedance may result in voltage stresses which will exceed the single-shot breakdown voltage of the insulation of low-voltage equipment. This may explain many cases of meter failure which have occurred in rural areas of high lightning intensity. Furthermore, with or without interconnection of lightning arrester and secondary grounds, great danger to life can result from this characteristic of the impulse impedance.

The relation between the inductance, capacitance, contact resistance, and leakage resistance of the ground circuit explains the *V* characteristic of the impulse resistance-time curves in Figures 11 to 15 of the author's paper. As the point of series resonance is passed, the impulse impedance of the circuit increases, and the impedance again approaches the 60-cycle value.

This *V* characteristic would be experienced, irrespective of the effect of voltage gradient. With much lower values of impulse currents, where the effect of voltage gradient would be minimized, Towne in his earlier work obtained similar results.

A need is apparent for further research in this field. Work on the evaluation of the ground-circuit constants for the various type soils and ground conductors is desirable. This information will be of great value in the future study of the protection of apparatus from lightning. It is hoped that the author's further investigation of the impulse characteristics of driven grounds will shed some light on the relationship between the circuit constants.

W. E. Berkey (Westinghouse Research Laboratories, East Pittsburgh, Pa.): The authors explain the lowering of the effective resistance of an electrical ground under impulse test in terms of an increase in effective area of the grounding electrodes caused by an electrical breakdown of the ground due to high-voltage gradients. With this assumption they are able to calculate the ground resistance of single or parallel grounds under impulse test conditions with a knowledge of the soil resistivity, the impulse current, and the dimensions of the rod.

It seems probable that the mechanism of conduction through the soil is mainly electrolytic. It is well-known that in electrolytes the conductivity increases (or the resistivity decreases) when the voltage stress is increased. Dilute solutions show a larger increase in conductivity than more concentrated solutions. The increase of conductance depends upon the mobilities, valences, and concentration. This is known as the Wien[1] effect and is not due to temperature or heating effects. For example, in Table I is shown the Wien effect for a barium ferrocyanide solution with conductance $k=0.000125$ ohm^{-1} centimeter^{-1} or resistivity = 8,000 ohm-centimeters. The electrode spacing was 0.44 centimeter, and the time constant of the spark discharge was 10 microseconds.

This effect, of course, varies with different electrolytes but may account for the observed lowering of ground resistance in a qualitative way, when it is remembered that probably the conduction through the earth is through electrolytic film-coated particles, in which the voltage gradients may be much higher at the particle contacts than over the particle itself. Debye and Falkenhagen[2] discovered an increased conductance due to an increase in frequency in electrolytic conduction. The conductivity of soil was found by Banerjee and Joshi[3] to increase with frequency of radio waves.

Thus, it would seem possible to account for the lowering of the impulse resistance by assuming that the effective resistivity of the soil is a function of the voltage gradient. Have the authors considered this approach? In the case of ground *M* shown in the oscillogram of Figure 6 of the paper a real breakdown occurs, in which some of the particles actually flashed over, due to excessively high gradients caused by the abnormally high soil resistivity.

Table I. Wien Effect for $Ba_3Fe(CN)_6$

Kv	$\frac{R_x}{R_0}$	Kv X/Cm	
4.47	0.94	10	Data fits curve $R=AX^2-BX^4$ where *A* and *B* are constants
7.96	0.83	18	
11.3	0.74	25	
14.3	0.65	32.5	
17.4	0.57	39.5	
20.3	0.50	46	
26.1	0.41	59	
31.7	0.33	72	
37.3	0.28	85	

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2. DISPERSION DER LEITFÄHIGKEIT UND DER DIELEKTRIZITÄTSKONSTANTE STARKER ELEKTROLYTE, **P. Debye** and **H. Falkenhagen.** *Physikalische Zeitschrift*, volume 29, 1928, page 401.

3. DIELECTRIC CONSTANT AND CONDUCTIVITY OF SOIL AT HIGH RADIO FREQUENCIES, **S. S. Banerjee** and **R. D. Joshi.** *Philosophical Magazine*, volume 25, series 7, 1938, page 1025.

P. L. Bellaschi, R. E. Armington, and A. E. Snowden: The authors want to thank all who have discussed the paper for their contribution to the subject.

Mr. Berkey cites interesting data on the lowering of the conductivity of a barium ferrocyanide solution with increasing voltage gradient. It is suggested that the resistance of grounds in soils may be affected to the same extent by purely electrolytic action. The results of impulse tests we have made on tap water having a resistivity (2,200 ohm-centimeters) which is representative of the ground water at the site of the grounds show that the resistance of the water is practically constant, independent of the voltage gradient. Impulse currents of a 15x30 microsecond wave were applied to electrodes immersed in the water and both current and voltage oscillograms recorded. The measurements cover gradients up to 28 kv per centimeter. The water broke down at 50 kv per centimeter. Within the range of experimental accuracy, all these data show that the resistance of the water remains constant.

The influence of the electrolytic effect in lowering the impulse resistance of natural grounds commonly encountered, similar to those investigated, does not appear to be of more than secondary importance. Exception to this would probably be the case of low-resistance grounds, such as grounds in fills and artificial grounds treated with salts for the specific purpose of lowering the resistance. That is, the exception would be the low-resistance grounds—a point Mr. Beck also brings out in his discussion.

The importance of geology, soil structure, and similar factors in determining the resistance of grounds in a given location, to which Mr. Beck refers, will require a good deal more attention and study. For the problem is of such wide scope that a clearer and more complete understanding of the underlying factors hardly can be reached without a combined approach of field experience, analysis, and actual tests.

As further progress is made, some of the factors Mr. Treadway suggests may be segregated and their respective influence and part in determining the characteristics of grounds better established. However, a good deal more experimental data will be required. In the opinion of the authors, contact resistance and capacitance are factors of secondary importance. There are some exceptions to this statement. For instance, grounds do have a capacitance effect which in very high-resistance soils such as rocks, has a certain contributing effect on the characteristic of the grounds.

Again, the capacitance is a factor of some importance when an abrupt front is considered, particularly in the initial stage of the front discharge.

We agree with Professor Harding that many a complex technical problem has been tackled effectively and economically in the laboratory. The complex nature of ground presents problems which are conducive to fundamental approach in the laboratory. Gradually, the investigation should be extended to the complex forms which the practical problem of grounds in the field present. We always favor correlating field and laboratory results. To accelerate progress and because of favorable conditions, the method and approach presented in the paper were followed. That is, grounds such as employed in service were driven in various soils adjacent to the laboratory and tested at current discharges corresponding to actual lightning.

Shielding of Substations

Discussion and authors' closure of paper 42-14 by C. F. Wagner, G. D. McCann, and C. M. Lear, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 96–100.

G. D. Floyd (Hydro Electric Power Commission of Ontario, Toronto, Ont., Canada.): The degree of protection given to a substation by masts or wires or by a combination of these has hitherto been largely conjecture. This paper should remove some of the unknown factors and permit design of shielding on a somewhat more sound basis. I do not think it is the authors' intention that the data presented should be taken too literally. For example, are not the curves for 0.1 per cent exposure so close to the vanishing point that for all practical purposes all points lying on or below them constitute perfect shielding? Some details of the method by which the authors arrived at a curve where only one stroke per thousand contacted the protected object would give the reader a better idea of the use which might be made of these data in design of shielding.

As a general rule the low-voltage low-capacity substation is not shielded deliberately; the data in this paper offer an explanation. This type of substation covers only a small area and is generally designed to have relatively low elevation. Both factors contribute to the relative immunity enjoyed by such a station. It is rather a nice point to decide whether shielding is justified in the first place, and having decided that it is, to determine the degree in which it should be applied. Whether one per cent exposure is the maximum to be permitted is questionable. I doubt if any substations have such excellent shielding, unless it has been acquired indirectly.

A very common form of shielding is by means of overhead grounded wires, supported either on extensions of the main station structures, or on separate masts. It will usually be found that this combination is cheaper than masts alone for the same degree of protection and can, in addition, be combined with the overhead ground wires of incoming and outgoing lines, to protect these lines as well as the live parts in the station proper. While the data of this paper would appear to cover this combination, I think the advantages of this arrangement might have been shown a little more in detail.

For a given coverage masts and wires are more economical than masts alone and, in certain cases, are also cheaper than structure extensions. A standard height and design of mast is also economical, regardless of the configuration of substation. The required shielding is provided in this case by adjustment in location and number of masts.

It is not quite clear that the cross-hatched areas of Figures 11c and 11d of the paper represent the protected areas for 0.1 per cent exposure. If the locus shown in Figure 11a represents the approximate limit for two masts, should not the corresponding total area for three and four masts be the superposition of the areas between each mast and every other mast? If this be correct appreciable areas shown protected in Figure 11c and 11d would actually be unprotected on the basis of 0.1 per cent exposure.

C. F. Wagner, G. D. McCann, and **C. M. Lear:** Mr. Floyd's first point is concerned with the choice of 0.1 per cent exposure as a basis for application. Because of the vagaries of lightning the phenomenon involved is statistical in nature. While it is possible to install a network of parallel overhead ground wires, so that perfect shielding is attained, the more conventional construction that usually consists of one or two ground wires leaves large gaps through which a stroke might "infiltrate." It is difficult to assign limiting configurations to such construction that just provide perfect shielding. This may be seen from Figures 3 through 7 in the paper which show considerable displacement between the 1.0 per cent and 0.1 per cent curves. Had sufficient data been taken so that 0.01 per cent curves could have been plotted, some displacement between these curves would still exist. The laboratory difficulties encountered in obtaining such data are evident, inasmuch as to provide data on one point for 0.01 per cent exposure would require something of the order of 100,000 shots. Thus it becomes impractical to obtain data on perfect shielding. The authors have, therefore, chosen the value of 0.01 per cent exposure, as it represents a low enough value upon which data could be obtained and, at the same time, a reasonable value for design purposes. Even these data involved some extrapolation.

The authors agree with Mr. Floyd in the opinion that supporting masts with overhead ground wires are, in general, more economical for the shielding of large areas than masts alone. Some station design engineers, however, fear the danger incident to the breakage of such overhead conductors. It would seem that this difficulty could be alleviated by sufficiently heavy construction.

The question has also been raised regarding the extent to which all points within the shaded areas of Figures 11c and 11d of the paper are protected. These areas were derived from general considerations provided by the test data. Since presentation of the paper, additional tests have been made which definitely establish that all points within the shaded areas have an exposure not over 0.1 per cent for the range of variables given in Figure 10c.

The Influence of Towers and Conductor Sag on Transmission-Line Shielding

Discussion and author's closure of paper 42-37 by R. W. Sorensen and R. C. McMaster, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 159–65.

C. F. Wagner and **G. D. McCann** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): A paper presented before the Institute in 1940 by Wagner, McCann, and MacLane discussed the factors that must be correctly simulated in the laboratory so that tests on scale models can be used to obtain data regarding shielding. This paper also described test results concerning the shielding of transmission conductors by ground wires. This work has been extended by two papers presented at this session—one by Sorensen and McMaster which considers the shielding afforded by the presence of the towers and by the sag of the ground wires and conductors, and the other by Wagner, McCann, and Lear which considers the shielding of substations.

The data presented by Sorensen and McMaster is interesting in that it shows, for the particular configuration assumed, the extent to which the tower structure shields the conductors. For distances approximately two times greater than the tower height, little shielding is provided. This effect is epitomized in the curve of Figure 24 of the paper which presumes to indicate the variation of the integrated shielding effect for different span lengths. The ordinate of this curve is labeled "Per Cent Added Protection," which, expressed more clearly but not so concisely, might be called "The Per Cent Decrease of Strokes to the Conductor Because of the Presence of the Tower." The curve in Figure 24 is based upon the assumption that the relative shielding of the tower, as given in Figure 22 of the paper, is independent of the span length. Actually, however, this is not the case. As shown by Figure 7 of the paper entitled "Shielding of Substations," substantially perfect shielding should be obtained for span lengths less than twice the conductor height. The full

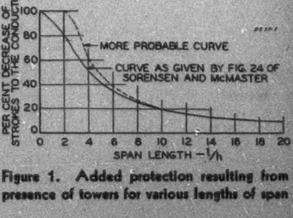

Figure 1. Added protection resulting from presence of towers for various lengths of span

line of the accompanying Figure 1 is reproduced from Figure 24 of the Sorensen and McMaster paper, and the dotted line indicates the more probable curve resulting when an estimate of the change in distribution with span length is taken into consideration. The normal range of span length to tower height lies between 6 and 12, between which values the presence of the tower reduces the strokes to the conductor between 15 and 40 per cent. Thus the previous work on the shielding of conductors by ground wires is conservative to this extent. This represents about the same order of accuracy as one would expect upon extrapolating laboratory results to actual systems. It is assuring, however, that the error by neglecting tower effects is on the side of conservatism. Correction for this effect involves an increase of but a few degrees in shielding angle, which is hardly worthy of consideration.

The curve and the data presented by Sorensen and McMaster apply only to the particular configuration tested which represents a shielding angle of 64 degrees. While this particular configuration was chosen to magnify the effects involved, it would have been interesting to have determined the tower effects for a smaller shielding angle, an angle between 35 and 45 degrees which is more representative of good practice.

Robert C. McMaster (California Institute of Technology, Pasadena, Cal.): Since the writing of this paper, a further investigation of the shielding effect of towers on short transmission-line spans has been completed. Results of model tests conducted under conditions identical to those reported in the paper, for a span of length equal to twice the tower height, are shown in the accompanying curves. Two identical model towers (see Figure 6 of paper for detailed dimensions), each 10 inches in height, were placed 20 inches apart and supported taut conductor and ground wire at a protective angle of 64 degrees as shown in Figure 7 of the paper. Cloud height was five times tower height, as before.

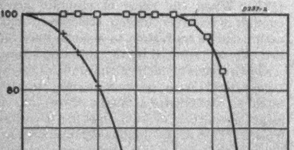

Figure 2. Stroke distribution in plane $B/h=0.5$, for span lengths of $2h$

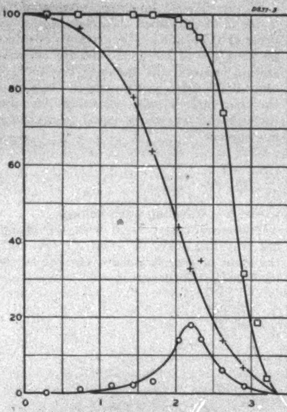

Figure 3. Stroke distribution in plane $B/h=1.0$, for span length of $2h$

With the cloud electrode in the plane of the tower, $B/h=0$, the distribution of Figure 8 of the paper was obtained for strokes to the system. With the cloud electrode in the plane $B/h=0.5$ (one-quarter the distance between towers) there was obtained the stroke distribution of Figure 2 of this discussion. In the plane $B/h=1.0$ (half the distance between towers) the distribution was found to be that shown in Figure 3 of this discussion. From the areas under these curves, Figure 4, showing the distribution of strokes along the span, was obtained.

The per cent added shielding, resulting from the presence of towers, is found by comparing Figure 4 of this discussion with Figure 21 of the paper, and equals 88.9 per cent for this span length. The result obtained from the investigation of longer spans, shown in Figure 24 of the paper, gave 80 per cent as the added shielding for this case, a conservative value which differs from 88.9 per cent by an amount which would have negligible effect upon the shielding calculated for an entire transmission line in which such short spans would be rare. Figure 5 of this discussion shows this correction to Figure 24 of the paper.

It is to be noted again that the results given in this discussion and in the paper apply quantitatively only for the 64-degree protective angle chosen for the test measurements, as stated in the paper. For other protective angles, the trends and relative shielding effects of towers and line sag can be determined only qualitatively from these data.

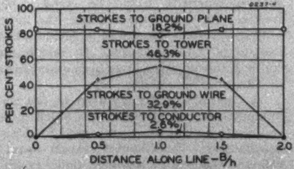

Figure 4. Distribution of strokes along line

Taut parallel ground wire and conductor with towers in place. Span length $2h$

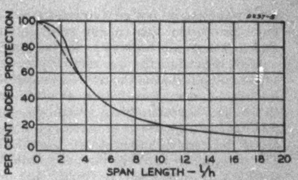

Figure 5. Added protection resulting from presence of towers, for various lengths of span, showing small correction at $l/h=2$

R. W. Sorensen: The several papers of the session have each made a definite contribution to our knowledge of lightning and what to do with it in connection with the operation of electric power systems, and structures to be protected. In 1926, some costly fires due to lightning occurred in oil-storage reservoirs, and a rather hurried study was made to determine the value of rods or towers as a means of providing protection against damage by lightning. One result of that study was the erection in southern California and other places of well-grounded towers to serve as lightning rods around oil-storage reservoirs.

In general, the towers were located so that any spot to be protected came within a circle, such that the distance from the rod on the level of protection was within $2^{1}/_{2}$ times the rod height from the rod. In other words, each tower is expected to protect around it an area included within a circle having a radius of $2^{1}/_{2}$ times the tower height. It is obvious that such a tower could not be in the center of the structure whose protection is desired. Therefore, in each installation, several towers surrounding the object to be protected were used.

These towers were placed at considerable distance from the reservoir to be protected, to guard against side flashing from the tower to the reservoir.

Naturally, the protective circular areas for the several towers were made to overlap so that all parts of the object whose protection is desired will have the protection of one or possibly more rods. No guarantees were made that absolute protection could be provided by rods, but up to the present time (15 years have elapsed) all places provided with the protection planned have been free from damage by lightning. The statistical data are therefore favorable.

The paper, "Shielding of Substations," by Wagner, McCann, and Lear, adds greatly to the information that was obtained when the protective plan for the oil-storage reservoirs was developed. Their presentation, combined with the information contained in each of the papers presented at the session, well augment our knowledge as to what can be done in maintaining lightning protection by the use of rods—in some cases supplemented by connecting grounded conductors.

A comparison of the data presented in McMaster's discussion, which show some slight changes from the data in the original paper, indicates that the original data regarding the amount of shielding due to towers and sag do not give an impression of more protection than might normally be expected in power lines with shorter spans.

With regard to the dependability of results as obtained from the tests on models as discussed in our paper, may I call attention to the fact that in studying actual lightning performances, as well as in the use of model tests, the results obtained are statistical. The probability law regarding the ratio of strokes that hit the rod, to strokes that hit elsewhere, holds both in model tests and in actual observation of lightning itself.

It therefore seems to me that, notwithstanding our inability to exactly duplicate all actual conditions with small scale models, the statistical measure of protection provided by the tests on models is well within the range of the statistical measure of protection upon which one can rely in using data as they apply to actual lightning. It therefore does not seem unreasonable to assume that we are fully justified in considering our model tests as indicative of actual lightning conditions, due regard, of course, being given to a reasonable engineering tolerance factor.

Lightning Investigation on 132-Kv Transmission System of the American Gas and Electric Company

Discussion and authors' closure of paper 42-18 by I. W. Gross and G. D. Lippert, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 178–85.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors have presented a large amount of data accumulated over the last eight years during the course of their lightning investigations. These data should be very useful to the utilities as well as to the designers of equipment, since the large amount of data available and presented permit drawing rather definite conclusions as to the severity of lightning currents, the effectiveness of ground rods and counterpoises and the effectiveness of modern lightning arresters.

The authors mention, in connection with Table II, that the apparent line-surge impedance is of the order of 236 ohms or less when the calculation is based on the measured bus voltage and on the measured line current. Conversely the traveling wave voltage using line current at station and surge impedance of 400 ohms is of the order of from 800 to 6,000 kv.

I would suggest the following interpretation:

If traveling wave theory is used on a circuit represented by an incoming line and outgoing line with an arrester at the junction, and the arrester is represented by a resistance corresponding to its maximum IR drop, the effective station resistance R' is a parallel connection of outgoing line-surge impedance Z_2 and arrester resistance R or $R' = R \times Z_2/R+Z_2$.

In the case of the currents presented in Table II of the paper, the IR drop is of the order of 315 to 355 kv, the arrester resistance between 140 to 260 ohms, and the effective station resistance R' between 105 and 157 ohms if Z is taken as 400 ohms. The sum of the currents in the outgoing line and through the arrester is equal to measured line current.

$$i_R' = \frac{2E}{R'+Z}$$

where E is traveling wave voltage.

The current associated with the incoming traveling wave is $i_t = E/Z$; therefore, the ratio of traveling wave current to the total measured current is

$$\frac{i_t}{i_R'} = \frac{R'+Z}{2Z}$$

or

$$i_t = i_R' \frac{R'+Z}{2Z}$$

i_R' represents the sum of the incident and reflected traveling current wave which is measured as line current. R' and Z are known impedances, and, therefore, the traveling wave current, the traveling wave voltage, the current in the arrester and in the outgoing line can be calculated. Table I of this discussion shows these values for the five cases at Roanoke.

Therefore, the bus voltage represents the sum of the incident and reflected traveling voltage wave; likewise, line current represents the sum of the incident and reflected traveling current wave. Bus voltage divided by line current gives the value of the effective terminal impedance of the line.

The traveling wave voltages range between 560 and 800 kv which are reasonable values and are to be expected near a station on a 132 kv system.

The bus voltages measured are, of course, at least as high as the arrester-gap breakdown, which is 388 kv for AIEE lightning-arrester test. The IR drop following the arrester-gap breakdown is however, much lower for the low currents indicated in Table I of this discussion. The effective terminal impedance depends on the IR drop of the arrester rather than on the gap breakdown voltage.

The calculated and measured arrester current checks well for case 3 only. In all other cases, the calculated currents are higher than the measured values from 700 to 2,000 amperes. It is not stated whether the measured line current is the sum of the currents on the incoming and outgoing line. If it were the sum, then, of course, the calculated arrester currents would be considerably smaller. In case 3 for instance, the lightning arrester current would be 800 amperes instead of 1,400 amperes. In calculating Table I, line currents have been assumed to be measured on one line only.

The actual circuits involved in the terminal impedance at stations is much more complicated than the circuit used for these calculations. Especially if station capacitance is taken into account, the traveling wave current would be roughly one-half line current for relatively steep current waves, because the capacitance initially reacts as a short circuit to the incoming current wave with a consequent reflected wave of amplitude and polarity equal to the incident wave. On that basis, the calculated arrester currents check more closely the measured currents, and the traveling wave voltages will range between 400 kv and 640 kv for the examples in Table I of this discussion. A more exact calculation can be made when all the constants of the circuit are known.

The low bus voltage of 300 kv in the fourth case at Roanoke is indicative of a slow rate of voltage rise of the traveling wave. This is substantiated by the maximum rate of voltage rise of 92 kv per microsecond given in the table for this case, the calculated IR drop is 320 kv with an arrester current of 1,600 amperes. In the fifth case at Roanoke, the high line current and low bus voltage without arrester operation suggest that the incoming traveling wave was of the order of 800 kv or higher but of short duration, probably less than one microsecond. Such a wave could be reduced by the station and bus capacitance to the measured value of 233 kv and, therefore, would not cause arrester operation. It would be interesting to know whether records indicate a line flashover at the time this record was obtained. A short wave of only one-microsecond length would, however, have a relatively steep front of the order of 800 kv per microsecond or greater, while the wave-slope indicator indicated only 164 kv per microsecond.

Similarly, the high bus voltages shown for cases 1 and 2 at Roanoke would indicate high rates of voltage rise to exceed the arrester-gap breakdown voltage of 388 kv.

The rates of voltage rise indicated in the Table II of the paper for a few of the cases in Roanoke, therefore, seem to be too low to account for some of the other simultaneous observations in the station and on the line.

If a wave-slope indicator, such as shown on Figure 1A of the paper was used in this station, the calibration of the instrument would have a dual calibration on account of the 35-turn coil which probably has an inductance of the order of 13 microhenries and the fact that a protective gap is provided. With rates of rise producing inductive voltages with amplitudes smaller than sparkover of the gap, the calibration should be $de/dt = i/2C$. When the rates of rise are great enough to arc-over the gap, the current indication can be equivalent to several times the rate of rise indicated by the above equation.

On the basis of such calculations, it might be worthwhile to re-examine the records. Such an examination might show that a larger proportion of waves had steeper wave fronts. Due to the dual calibration characteristic, the correct rate of rise can be established only in such cases where other related data, such as in Table II of the paper, are available.

Finally, with respect to the authors' Table III, I would suggest a possible explanation for the apparent greater effectiveness of the tower legs to carry current than the counterpoise. If we analyze case 1 of Table III, we find that the tower-base voltages associated with tower leg, ground rod, and counterpoise currents are 360,000, 275,000, and 117,000 volts respectively if currents are multiplied by resistance. Obviously, the three voltages cannot exist simultaneously. Now we know from tests[1,2], that a counterpoise resistance varies between some 100 to 200 ohms initially to a value somewhat lower than the d-c resistance, provided the current fronts

Table I. Lightning Data at Roanoke Station as Calculated and Compared With Test Results of Table II of the Paper. Z = 400 Ohms

Measured Line Current (Amperes)	Measured Arrester Currents (Amperes)	Calculated Arrester Currents (Amperes)	Bus Kv Measured	IR Drop Calculated	Terminal Impedance Calculated	Traveling Wave Current (Amperes)	Traveling Wave Voltage (Kv)
2,000	500	1,220	472	314	157	1,400	560
3,200	350	2,360	525	336	105	2,000	800
2,200	1,350	1,400	397	322	146	1,500	600
2,400	450	1,600	300	320	133	1,600	640
2,000		No operation	233	314	157	2,000	800*

*Arrester did not operate. Indicates chopped traveling wave reduced to 233 kv by station capacitance.

are steeper than the length of travel of the current wave on the counterpoise. On the other hand, the 200 ohms resistance of the tower leg probably does not vary considerably since its length is short (ten feet) and the registered current of 1,800 amperes is relatively low. The resistance of the ground rod probably is initially higher than the d-c value, since its length of 60 feet is considerable.

Calculations based on these considerations would indicate a probable maximum rate of current rise of the total current to ground of 20,000 amperes per microsecond and an average rate of rise of 4,000 amperes per microsecond with a crest current of 10,000 amperes. In other words, the three current crests measured do not occur at the same time.

The rates of rise indicated above are well within the range of measurements made on the Empire State Building.[3] A rate of 20,000 amperes per microsecond or more was recorded in 36 per cent of the records. Since the values shown in Table III are undoubtedly the results of direct strokes, the results of such measurements seem to indicate a qualitative check to the measurements at the Empire State Building. If the data of Table III are interpreted in this manner, that the apparent high tower-leg conductivity indicates high rates of current rise, such measurements might be useful to obtain statistical data on rates of rise of lightning currents.

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1. COUNTERPOISE TESTS AT TRAFFORD, C. L. Fortescue and F. D. Fielder. AIEE TRANSACTIONS, volume 53, 1934, July section, pages 1116–23.

2. THEORY AND TESTS OF THE COUNTERPOISE, L. V. Bewley. AIEE TRANSACTIONS, volume 53, 1934, August section, pages 1163–72.

3. LIGHTNING TO THE EMPIRE STATE BUILDING, K. B. McEachron. AIEE TRANSACTIONS, volume 60, 1941, September section, pages 885–90.

W. W. Lewis (General Electric Company, Schenectady, N. Y.): In Figure 10 of the paper by Messrs. Gross and Lippert are given curves of rate of change of voltage at stations. These curves are based on a certain interpretation of the wave-slope-indicator records.

In a paper by Lewis and Foust, entitled "Lightning Investigation on Transmission Lines—IV," which appeared in AIEE TRANSACTIONS for August 1934,[1] Figure 1 shows the time to reach crest of 45 waves caused by lightning on the 220-kv Wallenpaupack–Siegfried line, as recorded at the end of the line by cathode-ray oscillograph, and Figure 3 gives similar information for 16 waves recorded at the mid-point of the line. I have taken the same data and calculated the kilovolts per microsecond of the wave front.

In Figure 1 of this discussion curve *A* shows kilovolts per microsecond as ordinates against percentage equal to or greater than ordinate for 44 waves recorded at the end of the transmission line near the station. Curve *B* gives similar information for the 16 waves recorded at the mid-point of the line. It will be noted that curve *B* lies considerably above curve *A*. Three points of small voltage amplitude but very short time to crest account for the upper part of curve *B*. A possible explanation for the difference in these curves is as follows: The transmission line was covered with two overhead ground wires for 20 miles near the end of the line. Most of the waves recorded traveled in toward the station from a long distance and consequently suffered considerable attenuation before reaching the oscillograph. At the mid-point of the line, however, there were no overhead ground wires and it is possible that the waves originated nearer to the oscillograph. Consequently, these waves were steeper than the waves at the end of the line.

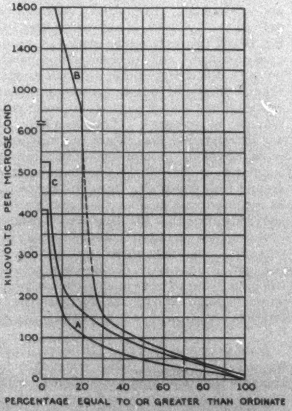

Figure 1. Comparison of wave-slope-indicator and cathode-ray-oscillograph records

Curve *A*. 44 cathode-ray-oscillograph records at end of Wallenpaupack–Siegfried line

Curve *B*. 16 cathode-ray-oscillograph records at mid-point of Wallenpaupack–Siegfried line

Curve *C*. 554 wave-slope-indicator records at stations on Glenlyn–Roanoke line

Curve *C* is plotted from 554 records secured on the Glenlyn–Roanoke line in 1939, 1940, and 1941, by means of the wave-slope indicator. These are the same data on which Figure 10 of the Gross-Lippert paper is based, but the data have been interpreted differently. The oscillatory surge interpretation has been used, making use of the data from all of the links.

The data of curves *B* and *C* were both taken under similar circumstances, that is, from traveling waves coming into stations on conductors shielded by overhead ground wires. These two curves check very closely and this circumstance gives considerable confidence in the wave-slope indicator and the oscillatory surge interpretation.

REFERENCE

1. LIGHTNING INVESTIGATION ON TRANSMISSION LINES—IV, W. W. Lewis and C. M. Foust. AIEE TRANSACTIONS, volume 53, 1934, August section, pages 1180–6.

J. T. Lusignan, Jr. (Ohio Brass Company, Mansfield, Ohio): In commenting on the results of Table II of the paper the authors mentioned the apparent discrepancy in arriving at line impedance values. I assume that the bus voltage is measured next to the arrester so that applying this to the line current should result in a value for arrester impedance not line-surge impedance. Could that be a proper assumption?

In the paper I note reference to the use of $^{1}/_{16}$-inch-by-2-inch flat iron strap for counterpoise. What are the advantages of this section? I am wondering whether there have been any corrosion problems with such a section as yet.

Should the range of 15.6–180 per cent for relative conductivity of the ground rods not be 31–180 per cent? The last column of Table III leads me to believe this.

The field-investigation data described in this paper should prove most helpful not only to the operating engineer and line designer but also to the equipment manufacturer. I was very interested in the figures given by the authors for rates of voltage rise recorded at the stations. While it is comparatively easy to design line insulators to resist puncture under exceedingly steep wave fronts, it is rather costly to do so with such station insulation as bushings. Often some sacrifice of other worth-while properties must be made. So far we have designed to meet voltage rises in excess of 1,000 kv per microsecond. We note that the authors only measured a maximum of 600 kv per microsecond. I am wondering whether they felt that 1,000 kv are still a reasonable figure. It is not that we want to drop it any. We would just not like to have it hiked much higher.

C. M. Foust (General Electric Company, Schenectady, N. Y.): The wave-slope measurements of conductor voltages at substations included in the paper by Messrs. I. W. Gross and G. D. Lippert were obtained through an extension of our now extensively used surge-crest ammeter technique.[1] Several steps in this adaptation, particularly as to details of calibration not included in the paper, are brought out in this discussion.

The general arrangement of field setup as shown in Figure 1 of the Gross-Lippert paper consists of magnetic links located in the ground connection to the coupling capacitors to measure maximum capacitor currents. The design of the coupling-capacitor wave-slope indicator is based on the following considerations:

1. A range of wave slopes from 25 to 5,000 kv per microsecond.

2. Measurements of capacitor currents reversing in polarity with a wide range of relative front- and tail-slope ratios.

3. Coupling capacitors of several capacitance values.

For a unidirectional voltage wave on the line the coupling capacitor current will reverse in polarity, and when the charging and discharging currents are of the same order of magnitude, a simple unidirectional current calibration for the magnetic links will be inaccurate, giving values generally below true wave slopes.

These considerations were provided for through the design of an eight-link recording station for each coupling capacitor with the links set in or adjacent to small coils in the ground connection of the coupling capacitor as shown in Figure 1A. The coil turns and link locations were designed to give a magnetization ratio of 2.5 to 1 between adjacent links. On a surge-crest ammeter of 100 divisions and proportional to magnetization current, this 2.5 to 1 ratio gives at least three adjacent link readings for each application of unidirectional current from 10 to 8,000 amperes. This range of current provides for wave-slope readings from 25 to 5,000 kv per microsecond with the available coupling capacitors ranging from 0.00025 to 0.0015 microfarad. Now with this overlapping of link magnetization, at the known link ratio of 2.5 to 1, an oscillatory calibration similar to those we have used for structure currents and checked by actual laboratory tests can be used to obtain crest-current values with reasonable accuracy.

On this basis of interpretation our field records range up to 525 kv per microsecond with the distribution of frequency of occurrence shown in Table II. These values do not depart greatly from those presented by Gross and Lippert in Figure 10 of their paper and based on a somewhat different interpretation.

As far as available records indicate, these slopes were the result of traveling waves incoming to the station from strokes to the line some distance out. For comparison with these distant strokes we have a direct-stroke oscillographic record made in 1930 of slightly under 2,000 kv per microsecond. This 2,000-kv-per-microsecond slope was a direct stroke to a high-voltage transmission line, the measurement having been made immediately at the point of a stroke to the line conductor of a 220-kv line. No overhead ground wires were in use at the time. The point struck was out on the line remote from the terminal stations, and no additional station capacitance was available to reduce the wave slope. This value of voltage wave slope, therefore, possibly ranges above that to be expected on well-protected lines at substations.

Table II

Wave Slope Kv Per Microsecond	Frequency of Occurrence Per Cent Above Magnitude of Column 1
525	Maximum recorded
220	10
150	20
100	40
60	60
35	80
25	100

REFERENCE

1. DIRECT MEASUREMENT OF SURGE CURRENTS, C. M. Foust and J. T. Henderson. AIEE TRANSACTIONS, volume 54, 1935, April section, pages 373–8.

J. G. Hemstreet (Consumers Power Company, Jackson, Mich.): This paper presents very valuable information pertaining to the effects of lightning on high-voltage transmission systems. We compliment the authors on the splendid way that they have collected and presented such a large amount of pertinent data and concur on the conclusions that have been derived.

The statistical curves for lightning currents appearing in the various elements of a transmission system should be of interest to all concerned with lightning protection and performance of high-voltage transmission facilities.

The writer has been associated for the past five years with an investigation of the relative effectiveness of radial counterpoise and deep driven ground rods. This investigation has been conducted on towers situated on a sandy plane where tower footings and counterpoise offer very high measured resistances to ground. Deep driven ground rods extending to depths of approximately 100 feet below the surface offer very low measured resistances and are connected in parallel with the counterpoise to each tower. Counterpoise and rods are so equipped that contributions of each to the total tower current may be measured. It has been found in our investigation that where the measured resistance of the ground rods is very low as compared to that of the counterpoise, the ground rods contribute three to four times as much current as does the counterpoise, thus indicating that the deep driven ground rods are very effective for dissipating stroke currents. The ratio of per cent current to per cent conductivity of the counterpoise has been found to be as much as 8.6 or more when the same ratio for the combined tower footings and ground rods is in the order of 0.6.

Data presented in Table III of the paper for cases 1, 2, and 3, when rearranged to compare the counterpoise contribution with that of the ground rods and tower footings combined, give ratios of per cent current to per cent conductivity for counterpoise as 0.72 to 0.78, while the same ratio for ground rods plus tower footings is about 1.6 to 2.0. This differs somewhat from our experience and is probably due to the different conductivity relations of counterpoise and grounding system.

It is, therefore, possible that the counterpoise may contribute much more current than a measure of its conductivity would indicate, while means for grounding the towers directly by ground rods and tower footings would carry current more nearly in the order of their conductivities.

The writer is curious to know the general nature of the surface and soil conditions for the towers listed in Table III, since the measured resistances of the counterpoise are somewhat less than either tower footing or ground-rod resistances in every case.

W. G. Roman (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. In discussing Table *A* of the paper, the authors attempt to estimate the conductor voltage by multiplying the observed line currents by an assumed surge impedance of 400 ohms. This leads to erroneous results, as the current recorded in the line is not a single traveling wave current, but the totalized current consisting of all traveling waves including reflections from arresters, flashovers, and junction points, as well as the original traveling wave current. The actual line voltage is determined by the impedance of paths to ground particularly the tower and tower ground or the station arresters and ground.

2. The same comments apply to the attempt to calculate the line-surge impedance from the observed line current and bus voltage in Table II of the paper. The bus voltage is determined by the impedance drop through the station arresters, the station ground, and the drop from the bus to the arresters.

3. In Table II, there are discrepancies between the observed line currents and the arrester discharge currents. Is this due to currents leaving the station on other lines connected to the same bus? If so, are records of these currents available, and do the sum of all the currents into the station bus correlate with the observed arrester currents?

4. When a traveling wave enters a station, the rate of rise of voltage on the bus is not necessarily the same as would be measured on a line when the same wave passed. A typical station is a complex network of parallel capacitances from the bus to ground through leads of various lengths. These station constants determine the rate of rise of voltage on the bus for a given traveling wave entering the station. For a given traveling wave, the rate of rise will be higher in a small station having low capacitance to ground than in a large station having high capacitance to ground. Also, a station having more than one line connected to the bus will show lower rates of rise than a station having only one line on the bus. For these reasons, caution should be used in applying data on rates of voltage rise obtained in a given type of station to stations or equipment in general.

C. F. Wagner and **G. D. McCann** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): A single magnetic link can be used to determine the crest magnitude of a unidirectional current. When, however, the current is oscillatory or has values of opposite polarity subsequent to the crest, two or more links are required to estimate the crest magnitude of the current. The reason for this is the demagnetizing action of the current of opposite polarity.

A demagnetizing current of but one or two per cent of the original magnetizing current might produce an error of 25 to 50 per cent of the true indication, and larger currents larger errors.

The magnetic link in the wave-slope indicator described in the paper by Gross and Lippert will be magnetized by a current which is proportional to the rate of change voltage across a capacitor. The authors recognize the interference of switching surges in most of their links and limit their lightning measurements to the indications of a single link. Due to the complex nature of voltage surges, especially at terminal equipment, the magnetic links are, in all probability, subjected to demagnetizing influences subsequent to the maximum magnetizing force. This would, undoubtedly, throw considerable doubt upon the records so obtained. Most, if not all, of these devices were located adjacent to transformers protected by arresters. It is likely that the maximum rate of change of voltage occurs when the arrester gap breaks down. Curve I of Figure 10 of the paper is, therefore, more likely to be a measure of the rate at which the arrester breaks down, influenced, of course, always by the extent to which any subsequent rate of change of opposite polarity might affect the reading.

I. W. Gross and **G. D. Lippert:** In presenting the data given in this paper, the authors hoped that sufficient detail was included to enable others interested in the lightning field to analyze it and draw their own conclusions, which, it was recognized, might not in every case conform to that given in the paper. Comments along this line are certainly most welcome, as it is recognized, in exploring new fields in lightning field research with newly developed instruments and even with some of the older ones, there are discrepancies which have occurred and may continue to exist for some time to come.

Regarding the matter of correlating measured bus voltages with line currents and surge impedances, it is recognized that the technical solution of this problem involves complicated mathematics together with a knowledge of terminal constants which it is difficult if not impossible to obtain. In presenting data, therefore, such as given in Table II of the paper, it was realized in suggesting this analysis that complete correlation might not be obtained. However, it is impractical to attempt to calculate lightning voltages and impulses with any degree of certainty in a complicated network consisting, for example, of one incoming line, four outgoing lines—one being on the same tower circuit as the incoming, and, in addition, coupling capacitors, station capacitance, terminal equipment, and the like at the station.

Table II merely attempts to show what correlation, if any, does exist on a very simple basis. It is hoped that the presentation of the data in Table II and the discussions which have followed will lead to more extended work on this problem in an attempt to reach a much more simple and easy solution than now required by extensive mathematical calculations.

Mr. Hagenguth uses a relatively simple mathematical analysis which seems to have promise of placing a reasonable interpretation on the data. The currents recorded in the paper are actually measured currents in the line wires and are not a value obtained by the addition of currents in separate wires. In the fifth case of Table II, there was no evidence found that there was a line flashover associated with this record.

Regarding the possibility of the protective gap flashover affecting the results, there was only one field station where evidence of this gap flashing over was obtained.

The data obtained in our field investigation, when interpreted, give rates of current rise comparable to those obtained on the Empire State Building. It has always been a question in the minds of the authors whether or not it was possible to convert satisfactorily records in lightning strokes to a building such as the Empire State Building to practical application to an elevated transmission system such as a 132-kv line or circuit.

Dr. Lewis' comparison of rates of voltage rise measured at stations and out on the line with the data presented in our paper is most interesting, considering that they were obtained by two entirely different methods. We subscribe heartily to the suggestion that higher rates of voltage rise undoubtedly occurred near the region of the stroke. This means that on lines which are not shielded, higher rates of voltage rise may be expected than on lines partially or adequately shielded.

Regarding the use of galvanized iron strap for counterpoises on which a query has been made by Dr. Lusignan, this material was used on account of its low cost, expected freedom from theft, and the further belief that it would be reasonably unaffected by soil corrosion. Digging up and inspecting this material after some six years indicated that there was relatively no trace of any corrosion whatsoever.

The percentage range of 31 to 180 instead of 15.6 to 180 for the relative conductivity of ground rods is correct. Regarding the question raised by Dr. Lusignan as to whether the 1,000 kv per microsecond furnish a reasonable basis on which to design equipment, we believe that there is nothing in the picture as brought out from the data reported in the paper relative to natural rates of rise of natural lightning on transmission lines which would indicate that the 1,000-kv-per-microsecond figure is too low. Until further evidence is available, there certainly seems to be no justification for decreasing this value as a design basis.

The question of the method of determining rate of voltage change in the field with the surge-crest ammeter has been mentioned by Messrs. Wagner and McCann, and perhaps Mr. Foust's discussion has already cleared up this point. We recognize that this wave-slope indicator is a relatively new instrument in the field and will be subject to some scrutiny until records obtained therefrom have been correlated with other data obtained by different methods. Considering the relatively large amount of data that is obtainable by the use of this instrument at a reasonable cost, in comparison with a single cathode-ray field installation and the uncertainties coupled even with this more accurate measuring instrument, it is believed that the wave-slope indicator has yielded some very valuable information, although it may be later subject to some modification in the instrument itself or detailed refinement in calibration.

The results Mr. Hemstreet has mentioned in connection with his field work in measuring the effectiveness of driven ground rods, counterpoises, and tower resistances are interesting. The fact that they do not show the same trend as those reported by the authors may be due in part at least to different soil conditions. The conditions under which our reported measurements were made were in flat country with sandy loam-type soil. We hope to continue further investigation on this subject, and perhaps the results may show why there is a discrepancy between the grounding results shown in Mr. Hemstreet's field study and those reported by the authors.

Lightning Investigation at High Altitudes in Colorado

Discussion and authors' closure of paper 42-16 by L. M. Robertson, W. W. Lewis, and C. M. Foust, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 201–08.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The paper is very interesting from the point of view of extending lightning-stroke records into regions of high altitude. The conclusion is drawn that there may be no lightning strokes within regions of altitudes of 18,000 feet and above. I should expect that this conclusion is correct as far as currents of sizable amplitude are concerned. On the other hand, the existence of long continuous discharges of relatively long duration might be expected to take place between differently charged portions of the clouds, as well as between cloud and ground. Data are available. The fact that the terrain extends into the cloud should not preclude formation in the clouds, especially since air currents at such altitudes are frequently extremely violent.

It is interesting to note from Figure 1 and Table 1 of the paper that the average number of strokes per mile per year, over the whole section above 6,000 feet, is approximately 0.25; in the section 6,000–8,000 feet —0.43; 8,000–10,000 feet—0.1; 10,000–12,000 feet—0.17, and in the short section above 12,000 feet—1.75 strokes per mile per year.

In conclusion 2 of the paper, it is shown that of the 145 strokes registered, 56 per cent caused faults and 44 per cent caused no line disturbances. It would be interesting to know the distribution of faults with respect to the altitude on this line. From the data presented, it would appear that flashovers should occur frequently at altitudes, such as are attained by this line, because:

1. The number of strokes per mile is high.
2. The ground resistance of tower footing, and probably also of counterpoise, increases rapidly with altitude as shown in the paper.
3. The flashover voltage of the air decreases considerably at high altitude.

Therefore, the reduction in current amplitude should not have so great an effect on the fault record as would appear from the current data alone. I would appreciate the authors' comments on this phase of their investigation.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Robertson and his coauthors conclude that there is little likelihood of lightning strokes at altitudes of 18,000 feet or more. Although the data adduced in the paper are hardly complete enough to prove the case, there is some logic in the assumption that altitude influences lightning. Is it not likely that this influence varies with climatic conditions and geography? In the tropics, thunderstorms are reported to be of somewhat different nature than in the temperate zones, with a greater prevalence of strokes between clouds. This may be a manifestation of an effect as discussed by authors in so far as the freezing isotherms shown in Figure 10 of the paper are, no doubt, at higher altitudes than in the United States, so that the separated charges may lie at higher altitudes. It would be interesting if more data were made available on this subject.

Another factor that may color the distribution of the current records is the influence of earth resistivity and geology on the nature of the lightning currents that flow after the stroke has been established. Some lightning investigations and observations of arrester performance, as discussed before the Institute[1] last summer, indicate that such an influence exists. This is logical, since the earth, as well as the cloud, is a terminal of the lightning battery. In regions of ancient geology and high resistivity currents of lower amplitude may prevail. Such regions exist in Colorado, and the effect may be magnified on the tops of rocky mountains. The authors have mentioned geology, and continued investigation of its effect on lightning currents is of importance. This relates also to the paper by Messrs. Bellaschi, Armington, and Snowden, on impulse and sixty-cycle characteristics of driven grounds, presented at this meeting.

REFERENCE

1. **Discussion by Edward Beck** of LIGHTNING TO THE EMPIRE STATE BUILDING—II. AIEE TRANSACTIONS, volume 60, 1941, pages 1377–8.

C. F. Wagner and **G. D. McCann** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As stated by the authors, operating experience of transmission circuits at high altitudes over a period of 30 years has shown that there is less damage to equipment than at lower altitudes. The authors, from the data submitted, conclude that this is due to the absence at the higher elevations of lightning currents of large magnitude. It may be that the simple explanation is the less frequent occurrence of lightning strokes of all magnitudes; that is, the current distribution curve might be the same but the total strokes per season be fewer.

All the points obtained are tabulated in Table I and plotted in Figure 5 of the paper. For a given range in altitude, fewer records are obtained at high altitudes than at the lower altitudes. In the absence of information regarding the distribution of the number of instruments as a function of altitude, it is impossible to say whether this is due to fewer instruments at higher altitudes or fewer discharges per instrument. In any event, because of the statistical nature of such investigations, the number of records obtained above 8,000 feet is insufficient to warrant the conclusion that currents of large magnitudes do not occur. Had a larger number of instruments or a longer period of time been considered, currents of large magnitude might have been recorded.

The bench marks which form the points at zero elevation in Figure 5 were obtained from the low-altitude record of Figure 6 of the paper, the Lewis and Foust curve. The accompanying Figure 1 shows a similar curve obtained by Waldorf. If will be observed that this curve shows much smaller currents than that of the Lewis and Foust curve, and had the bench marks at low altitudes obtained from this curve been used, the straight lines of Figure 5 would have been entirely different.

In Figure 1 not only are the Lewis and Foust and Waldorf curves plotted, but also the mean curve for all the records of Table I, which represents data for altitudes in excess of 5,000 feet. Comparing this curve with the Waldorf curve shows that the effect of altitude is very small, the difference being much smaller than the difference obtained by different investigators at low altitudes.

Not being able to identify the particular points from which the different curves in Figure 6 were plotted, the authors replotted the data in Table I for the four ranges in altitude given. The results are shown in the accompanying Figure 2. These curves do not appear to indicate a very definite trend in the effect of altitude, the differences being smaller than are obtained from year to year or from season to season on the same line.

In conclusion, while the surmise of the authors may be correct that the magnitude of lightning strokes decreases with increase in altitude, the data presented appear insufficient to draw definite conclusions.

E. A. Evans (General Electric Company, Pittsfield, Mass.): The authors have stated in conclusion 1 that 36 per cent of the currents they measured were positive, while 64 per cent were negative. This is of considerable interest since, as they have also indicated, stroke currents measured on low-altitude transmission lines have been found to be roughly five per cent positive, and 95 per cent negative. They suggest as a possible contributing factor, the probability that the high-altitude lines may at times be within the clouds. Under this condition one might expect that the line would be nearer the upper positively charged portion of the cloud, and that it, therefore, would be more likely to receive discharges from it.

There is one section of the Shoshone–Denver line which I feel certain is seldom, if ever, within a thoundercloud. Therefore, a re-examination of the data on strokes to that part of the line will be of value in determining whether Colorado thunderstorms have charge distributions which differ from the distributions in low-altitude storms. The section referred to is that between 5,000 and 8,000 feet at the Denver end of the line. Judging from the authors' profile of the line, its length is about 27 miles.

This end of the line is subjected to the same types of thunderstorms which I studied from the top of Devil's Head Mountain, roughly 35 miles south of the line during the summers of 1930 and 1931. The two seasons were spent there, through the courtesy of the United States Forest Service, for the sole purpose of studying the phenomena occurring during thunderstorms.

Devil's Head Lookout is 9,348 feet above sea level, while the immediately surrounding country for some distance lies between 7,000 and 8,000 feet elevation. Although the lightning discharge centers of 37 storms passed within three miles of the lookout in 1930, and a number more in 1931, in no case was I within the thundercloud.

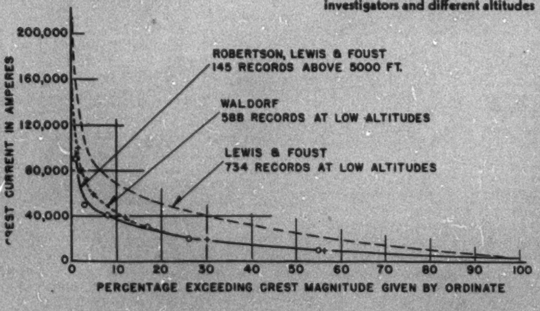

Figure 1. Comparison of current distribution curves for different investigators and different altitudes

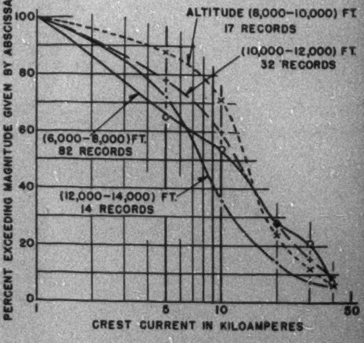

Figure 2. Cumulative curves of probable stroke currents as a function

Several measurements were made of the distance between ground level and cloud base. Most of these fell between 3,000 and 6,000 feet above the surrounding country and from 1,200 to 4,200 feet above the lookout. Only on very rare occasions were storms seen in which the cloud base appeared to be less than 1,000 feet above ground. These cases may have been optical illusions, for storms viewed at a distance, particularly when the rain area is dense, appear to be much lower than they actually are.

In view of this, it seems reasonable to assume that the section of line referred to was seldom within the cloud. Therefore, if a re-examination of data for it shows a considerably higher percentage of positive strokes than that obtained for storms at low altitude, a difference in charge distribution for low- and high-altitude storms may be indicated.

Based on my observations of thousands of lightning strokes, the shape of a number of which could be explained only by assuming that both positive- and negative-charge concentrations were present near the base of the cloud, and based on cloud field measurements, I am of the opinion that some of the positive currents measured by the authors were caused by discharges from centers of positive charge in the lower regions of the cloud.

Other positive records may have resulted from strokes which began as negative discharges from the base of the cloud to ground but ended as positive discharges, due to the discharge channels extending into the upper positively charged regions of the cloud. In Colorado on September 2, 1930, while watching a storm under conditions in which the cloud was clearly visible from its base to its top, I saw several discharges in succession which were probably of this type. In each case a stroke occurred from the lower part of the cloud to ground. Each stroke was followed by a progressive illumination upward through the cloud, which ended with several discharge channels visible near the top of the cloud. McEachron has recorded strokes to the Empire State Building[1] which started as negative discharges and ended as positive discharges. It is possible that the structure of Colorado mountain thunderstorms, resulting from their special conditions of formation, is such that this type of discharge occurs more frequently.

With regard to the decrease in current magnitude with altitude, the roughly sevenfold increase in tower-footing resistance between 7,000 feet and 11,000 feet possibly deserves some consideration. Using the tower-footing resistance as a crude measure of earth resistivity, a foot of counterpoise at 11,000-foot elevation should have of the order of seven times as much leakage resistance to ground as a similar length at 7,000-foot elevation. The difference in resistance offered to the discharge of a lightning stroke at the two locations will be less than this, due to breakdown of the air pores in the ground, but it should still be substantially higher at the 11,000-foot elevation. Considering the discharge of a stroke into a counterpoise (after Bewley[2]) as a combined leakage discharge and traveling wave phenomenon, the leakage resistance met by the stroke should be one that starts relatively high and continually decreases, as the surge travels along the counterpoise and thereby taps more and more paths in parallel to ground. Bewley's and Wade's[2] measurements of the velocity of propagation of an artificial lightning surge along a counterpoise indicate that after three microseconds between 900 and 1,000 feet of counterpoise should be picking up current from the earth. With this length of counterpoise in action, it appears doubtful that its resistance either at 7,000- or at 11,000-foot elevation would be high enough to substantially influence the maximum current of the stroke. Further evidence that a sufficient section of counterpoise probably comes into action quickly enough to offer a low-resistance path for the lightning current is given by one of the authors.[3] Still, in view of the nature of the ground under the Shoshone–Denver line at high altitude, ground resistance should not be entirely dismissed as a possible contributing factor toward the decrease in current with altitude found by the authors.

The authors' suggestion that decrease in temperature with altitude is a factor in limiting the formation of lightning is interesting. The rapid decrease[4] in moisture content of the atmosphere with altitude is also important. Based on the data given, it appears improbable that thunderstorms can form above an extended ground level of say 18,000-foot elevation, if the temperature at that level never exceeds 32 degrees Fahrenheit. Thunderstorms could, however, form at lower elevations and strike from the upper regions of the cloud to ground at that elevation as they moved toward it. Meteorologists believe, as shown by the generalized cloud diagrams in Figures 10 and 11 of the paper, that charge concentrations can occur in thunderstorms several thousand feet above the 18,000-foot level.

The study of corona discharge currents from points subjected to thundercloud fields appears promising as a means of increasing our understanding of the nature and strength of these fields. I would expect that refinements in calibration methods as the investigation proceeds will show the field strengths to be lower than those given in Table IV of the paper.

It should be possible to considerably increase our knowledge of the locations, polarities, and magnitudes of the charge concentrations causing thunderstorm fields by a continuation of the above measurements, combined with simultaneous observations of thundercloud position and characteristics, discharge center locations, and rain area locations. The records should also yield information on the charging processes going on within the storm cloud.

A few measurements of the discharge current from a 40-foot length of 0.011-inch-diameter bare wire, subjected to thundercloud fields, were obtained at Devil's Head in 1931. These yielded some interesting information on thunderstorm field characteristics. The maximum current which I measured was 314 microamperes. For this record a flow of positive electricity from wire to air was indicated showing that a negatively charged region in the cloud was dominating the field at the lookout.

REFERENCES

1. LIGHTNING TO THE EMPIRE STATE BUILDING, **K. B. McEachron.** *Journal* of the Franklin Institute, volume 227, 1939, pages 149–217.

2. THEORY AND TESTS OF THE COUNTERPOISE, **L. V. Bewley.** AIEE TRANSACTIONS, volume 53, 1934, pages 1163–72.

3. PICKUP AND RESISTANCE OF COUNTERPOISE SYSTEMS, **W. W. Lewis.** *Electrical World*, volume 116, November 29, 1941, pages 56–7, 102–03.

4. PHYSICS OF THE AIR, second edition, **W. J. Humphreys.** McGraw-Hill Book Company, New York, N. Y., 1929. Page 70.

L. M. Robertson, W. W. Lewis and C. M. Foust: Several of the discussers point out that the apparent decrease of lightning current with altitude may be due to causes other than those suggested by the authors, for example, climatic conditions and geography, influence of earth's resistivity and geology, and less frequent occurrence of lightning strokes of all magnitudes. Some or all of these points may have a bearing on the results.

Our data permit us to comment on the relative frequency of occurrence of strokes. All our results were obtained on the sections of line equipped with ammeter links, as indicated on Figure 1 of the paper, that is, on about 45 miles of the approximately 150 miles of line. With 145 total strokes recorded in four years, this would indicate about 0.8 stroke per mile per year. Bell[1] showed 356 towers flashed on the section of the Wallenpaupack–Siegfried line not equipped with overhead ground wires (about 36.9 miles) in a period of 13 years, or about 0.74 tower flashed per mile per year. Assuming each flashover represents a stroke, it will be seen that figures for high altitude in Colorado and low altitude in Pennsylvania are comparable.

In Mr. Hagenguth's discussion, he deduces from Figure 1 and Table I of the paper the average number of strokes per mile per year in the different altitude ranges. He overlooked the fact that data were secured only on about 45 miles of the line. The correct figures for strokes per mile per year would be approximately as follows:

Over the whole line	0.8
6,000 to 8,000 feet	2.05
8,000 to 10,000 feet	0.36
10,000 to 12,000 feet	0.4
Over 12,000 feet	1.17

Messrs. Wagner and McCann show a curve of lightning current against frequency plotted from Waldorf's data, and they compare this with the Lewis–Foust curve and with Figure 6 of the present paper. It is interesting to note that of the 734 records plotted in the Lewis–Foust curve, 457 records are from Waldorf's data on four systems. This is 62 per cent of the Lewis–Foust data and 78 per cent of Waldorf's 588 records plotted by Wagner and McCann. The remaining 277 records, or approximately 38 per cent of the Lewis–Foust data, are from four other eastern low-altitude power systems. From a comparison with various other curves we believe that the Lewis–Foust curve is representative of the range of current magnitude that may be expected at altitudes near sea level. It should also be noted, as mentioned in the paper, that 30 years operation indicated much less destruction, for example, shattering of wood poles and crossarms, at higher altitudes than at lower altitudes on the Colorado system.

We wish to thank the discussers for their interesting and constructive discussion. We intend to continue our investigation and hope to secure additional data which will confirm or revise our present conclusions.

REFERENCE

1. LIGHTNING INVESTIGATION ON A 220-KV SYSTEM—III, **Edgar Bell.** AIEE TRANSACTIONS, volume 59, 1940, pages 822–8.

Large Adjustable-Speed Wind-Tunnel Drive

Discussion and author's closure of paper 42-65 by C. C. Clymer, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 156–8.

M. M. Liwschitz (The Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): Mr. Clymer recommends in his very interesting paper that the rotors of both doubly fed machines in the wind-tunnel drive described by him be connected in series with the synchronous machine, in order to introduce a source of positive damping in the circuit of the doubly fed machines. Were the rotors directly connected in parallel, there would be only a negative damping in the circuit of the doubly fed machines, and these could oscillate independent of the total system without any restraining force.

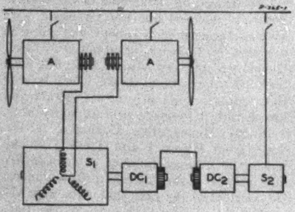

Figure 1

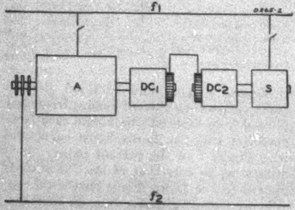

Figure 2

This statement by Mr. Clymer is correct as far as normal doubly fed machines are used, machines having the damping characteristics shown in the paper "The Doubly Fed Machine," presented at the AIEE winter convention by Messrs. C. Concordia, S. B. Crary, and G. Kron.[1]

But there is a possibility to build the doubly fed machine by using special windings, so that its negative damping is considerably reduced, and so that the damping torque of the system, consisting of the doubly fed machines and the fans, is not negative but slightly positive in the range between about 12 per cent slip and standstill. Doubly fed machines built in this way do not need a source of positive damping between the rotors and can operate without danger of nonrestrained oscillations, when their rotors are connected directly in parallel.

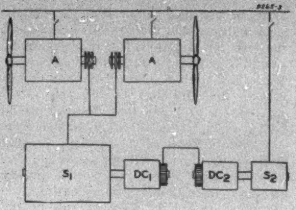

Figure 3

There is another point in the paper by Mr. Clymer on which I would like to make a remark. Mr. Clymer recommends the variable-speed drive as described in his paper for the use as frequency converter. For this purpose also the arrangement can be used that is shown in Figure 2. Two sets of this kind I have described in the *Siemens-Zeitschrift* 1926. The doubly fed machine is used here as a frequency changer only. The motors that drive the fans or pumps will be connected to the secondary line having the frequency f_2 and will be synchronous motors.

There is no danger of negative damping here, since the doubly fed machine is connected on one side with d-c machines, which produce positive damping, on the other side with synchronous machines which have damper windings.

Reference

1. The Doubly Fed Machine, C. Concordia, S. B. Crary, and G. Kron. AIEE Transactions, volume 61, 1942, May section, pages 286–9.

L. A. Umansky (General Electric Company, Schenectady, N. Y.): In reviewing these two papers, and in listening to the discussions, I was particularly impressed with one fact: a good and sound solution of an engineering problem depends not only on the kind but on the size of the equipment involved.

Take, for instance, the frequency changer described by Dr. M. M. Liwschitz. This arrangement is neat and probably is economical when the difference of frequencies between the two power supplies is small, and the capacity of the set is moderate. But suppose we must provide a 20,000-kw frequency changer to tie together a 60-cycle and a 25-cycle system. The d-c machines on the frequency changer and on the regulating set will be about 12,000 kw each. The high resulting cost is obvious; each d-c machine will have to be subdivided into three or four units. The whole arrangement will not be practical.

Or, let us take the remark made by Mr. Kilgore in reference to the slip-regulator control as a feasible application for wind-tunnel drives. Undoubtedly, for smaller drives of this kind, say of a few thousand horsepower, such control can be considered, particularly if a low efficiency is not a factor. But, for a 30,000-horsepower or a 40,000-horsepower drive, the matter of efficiency cannot be disregarded, and some means of slip-energy recovery should be provided. Furthermore, the slip-regulator control for a 40,000-horsepower drive will not solve the problem of starting current inrush which the described drives have successfully solved.

The system discussed here and first suggested by Mr. Clymer is not at all the Kraemer system. In the latter, as we all know, the slip energy of the main induction motor is converted into d-c power by means of a synchronous converter. Could this be done in case of 40,000 horsepower with a 6:1 or greater speed range? The capacity of the converter would be an equivalent of 30,000 kw; would have to be split into six or more units, and the problem of hunting, load division and so on, would be aggravated. Starting of the main drive would be just as difficult (considering the power system) as with a slip regulator.

C. C. Clymer: Mr. Liwschitz makes some very interesting observations concerning the possibility of obtaining damping on doubly fed machines operating in parallel. The use of a series connection for the rotor is, of course, only one way of achieving stability. There are other ways. For example, a six-phase synchronous machine, and the use of inductive coupling. The series connection is, shall we say, an economical and convenient method since it permits the use of perfectly standard induction motors with simply a reconnection of the synchronous machine.

Since Mr. Liwschitz does not describe the connection by which positive damping may be obtained with parallel rotor connections, a discussion on the various methods is not possible, however intriguing the subject may be.

Variable-Speed Drive for United States Army Air Corps Wind Tunnel

Discussion of paper 42-63 by A. D. Dickey, C. M. Laffoon, and L. A. Kilgore, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, March section, pages 126–30.

L. A. Umansky: See discussion, this page.

Equivalent Circuits for the Hunting of Electrical Machinery

Discussion and author's closure of paper 42-3 by Gabriel Kron, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 290–6.

S. B. Crary (General Electric Company, Schenectady, N. Y.): Kron's equivalent circuits for the damping and synchronizing coefficients of rotating machines already have been used in our company for many problems, and we can attest to their practicability and usefulness. We have obtained results which would never have been realized by ordinary numerical methods. Numerical methods may rapidly become impractical from an engineering standpoint when the number of factors to be taken into consideration increases only moderately. It may take more time to make an analysis in such a manner than it does to build the apparatus. This difficulty of the analytical method can be largely removed by methods which eliminate or simplify the numerical work. Network analyzers and differential analyzers provide means by which this may be accomplished.

The circuits developed by Kron for use on an a-c network analyzer may be used for an important class of problems: specifically, for the determination of the damping and synchronizing torques over the range of system conditions and design parameters. These parameters may include the frequency of oscillation, the effect of circuit elements to which the particular apparatus being studied is connected, and the effect of change in the design details of the apparatus or equipment itself. The method using damping- and synchronizing-torque coefficients allows a study of a particular part of the system and for particular modes of oscillation separately, without introducing the additional complexity of all of the other elements and all possible modes of oscillation at once. This then allows for a best design to be obtained for this particular element as it may be applied to different systems and under varying conditions. If necessary, additional tests for the stability and performance of the whole system may be made by using Routh's criterion, or a differential analyzer solution. The use of the circuits developed by Kron does not replace the use of other stability criteria and methods of analysis, but rather they increase their value by making them more easily applied.

Banesh Hoffmann (Queens College, Flushing, N. Y.): In his paper "Equivalent Circuits for the Hunting of Electrical Machinery," Kron transforms the nonsymmetric impedance of a machine to a symmetric form, which is then taken to be the impedance of an equivalent network. The manner of bringing Z to symmetric form has certain interesting features which seem worthy of discussion.

The process used by Kron has two essential steps:

A. **The introduction of complex quantities, and the application of a symmetric components transformation.**

B. **The subsequent multiplication by a diagonal matrix n'^{-1}.**

Let us discuss *B* first. In one place Kron regards this as the process of dividing each component equation of the vector equation of voltage by an appropriate scalar. In this form it has no tensor significance. I wish to show that the operation may actually be regarded as a valid tensor transformation, employing a well established tensor device known as a *mixed reference frame*.

Let the equation of voltage after step *A* has been performed be

$$e_a = Z_{ab} i^b \tag{1}$$

and denote the real, diagonal matrix n'^{-1} by $N_\lambda{}^a$. Since equation 1 is a vector equation, we may transform it to a new reference system using $N_\lambda{}^a$ as the transformation matrix. The tensor law of transformation gives e_a the new components

$$e_\lambda' = N_\lambda{}^a e_a \tag{2}$$

The *vector* $(Z_{ab}i^b)$, treated as a unit, will have the new components

$$(Z_{\lambda b}i^b)' = N_\lambda{}^a (Z_{ab}i^b) \tag{3}$$

It is thus a valid tensor transformation to write equation 1 in the form

$$e_\lambda' = N_\lambda{}^a Z_{ab} i^b \tag{4}$$

If we define

$$Z_{\lambda b}' \equiv N_\lambda{}^a Z_{ab} \tag{5}$$

we may write equation 4 in the form

$$e_\lambda' = Z_{\lambda b}' i^b \tag{6}$$

It is this quantity $Z_{\lambda b}'$ which Kron takes as his symmetric impedance. A full tensor transformation on equation 1, applying to each individual tensor therein, would give

$$e_\lambda' = Z_{\lambda\mu}' i'^\mu \tag{7}$$

with

$$Z_{\lambda\mu}' = N_\lambda{}^a N_\mu{}^b Z_{ab} \tag{8}$$

and

$$i'^\mu = N_b{}^{-1\mu} i^b \tag{9}$$

In equation 6 the e has been transformed to a new reference frame, the i has been left in the old, and the Z has been placed partly in one frame and partly in the other. The quantity $Z_{\lambda b}'$ is merely the impedance tensor Z_{ab} expressed in a mixed reference frame. Since $N_\lambda{}^a$ happens to be diagonal, the effect of the above transformation is merely to change the scales along the individual reference axes so far as the e's are concerned, while leaving the scales for the i's unaltered.

At this point let us recall the well-known theorem of tensor analysis which states that an unsymmetric tensor is unsymmetric in all reference frames. This theorem effectively prevents the transformation of Z to symmetric form by means of a full tensor transformation. But it is inoperative if we use a mixed reference system, and there seems no theoretical reason why the use of a mixed reference system should not alone suffice to bring Z to symmetrical form. On the other hand, in step *A*, by his introduction of complex transformations, Kron automatically takes the problem out of the restricted realm of tensor analysis into the more general one of spinor analysis. The impedance, when regarded as a spinor, has one dotted and one undotted index. For such a spinor symmetry is not an invariant property, the symmetry theorem being replaced by one which states that a hermitean spinor is hermitean in all reference frames, and a nonhermitean spinor nonhermitean in all reference frames. Thus, the introduction of spinors could theoretically alone suffice to bring Z to symmetric form. Nevertheless Kron uses step *A* merely to simplify the appearance of Z and makes Z finally symmetric by means of the mixed reference system introduced in step *B*. It would thus seem that he has been lavish in the theoretical resources used to combat the asymmetry of Z, since either the introduction of a suitable complex full spinor transformation alone or else the introduction of a suitable mixed tensor reference frame alone could theoretically make Z symmetric. When p is replaced by $j\omega$, and so on, the Z of a rotating machine and the Z of a network become alike in being nonhermitean, and this is an invariant characteristic, while the property of symmetry is neither invariant nor common to both. The use of a mixed reference frame obscures this invariant similarity. Therefore, in view of Kron's statement that only tensors (or more generally spinors) can be represented as measurable quantities in the equivalent network, it might ultimately prove of practical value to investigate the possibility of reducing Z to symmetric form by the use of a full spinor transformation only. The calculations required, though, would be of considerable length. Kron's main object was actually to make Z symmetric, and this he has done not only with elegance and real economy of labor, but also, as I have shown above, by the use of legitimate spinor operations.

The following remarks are the fruits of correspondence with Mr. Kron. They concern the reasons why the method by which Kron reduces Z to symmetric form is actually successful in accomplishing its purpose.

Initially we have equation 16 of Kron's paper, with R and L symmetric. If we apply a spinor transformation we are liable to destroy the symmetry of R and L. And if we do happen to preserve this symmetry under step *A*, we are liable to destroy the symmetry of R on multiplying it by n'^{-1} in step *B*. Clearly, therefore, the method of reduction cannot be generally valid. The following calculations show that it is actually made exactly to measure for the problem in hand.

By matrix multiplication we have

$$\begin{vmatrix} 1 & j \\ 1 & -j \end{vmatrix} \begin{vmatrix} a & c \\ c & b \end{vmatrix} \begin{vmatrix} 1 & 1 \\ -j & j \end{vmatrix} = \begin{vmatrix} (a+b) & , & (a-b)+2jc \\ (a-b)-2jc & , & (a+b) \end{vmatrix} \tag{10}$$

Thus only a diagonal spinor, originally symmetric, will remain symmetric under step *A*. Now R is always diagonal, provided there are no resistances common to any meshes, and L will be diagonal if there are no mutual inductances. But the transformation to symmetrical components is always applied to the d and q axes of a layer of winding, and since these are taken at right angles they have no mutual inductances. Thus step *A* does not disturb the symmetry of R or L. It does however change them from diagonal to nondiagonal form, as equation 10 shows. This is important since R', the new R, is multiplied in step *B* by the diagonal matrix n'^{-1}. By matrix multiplication we have

$$\begin{vmatrix} x & 0 \\ 0 & y \end{vmatrix} \begin{vmatrix} a' & c' \\ c' & b' \end{vmatrix} = \begin{vmatrix} xa' & xc' \\ yc' & yb' \end{vmatrix}$$

Thus $n'^{-1}R'$ will be symmetric only if either n'^{-1} or else R' is a multiple of the unit matrix. Now on the rotor it is assumed that the windings are symmetric. This means that R, and thus, by equation 10, R', is a multiple of the unit matrix, so that the condition is satisfied. On the stator, however, there may be asymmetries, and R is generally not a multiple of the unit matrix. But on the stator we have $v=0$, and this makes n'^{-1} a multiple of the unit matrix, thus fulfilling the condition in the other possible manner. Any asymmetries on the rotor, such as from an outside load, would destroy the effectiveness of the method.

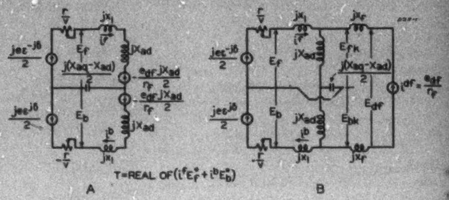

Figure 1. Two forms of the equivalent circuit of the salient-pole synchronous machine

T real of $(i^f E_f^* + i^b E_b^*)$

A—With impressed field voltage

B—With impressed field current

Gabriel Kron: Mr. Crary's discussion brings out rather emphatically the fact that analytical methods of investigating engineering structures become, from a practical point of view, valueless just when these studies are most needed, unless the method offers a means also for quick numerical solution. The need for and usefulness of calculating devices and models such as the a-c network analyzer is attested by the fact that the number of such devices is increasing, and in our company the analyzer is being worked two shifts.

The limitation of the hunting equivalent networks, as presented here, is that they are restricted to one frequency of oscillation. However, these circuits may be transformed so as to be valid for all frequencies, that is, for transients, such as the sudden short-circuit and acceleration phenomena of machines and interconnected systems.

Professor Hoffmann's points are all well taken. It is an excellent suggestion that the multiplication of $\boldsymbol{Z}$ and $\boldsymbol{e}$ with $\boldsymbol{n}'^{-1}$ should be considered as a transformation to a mixed reference frame, in which the transformation consists in changing the scale of the voltages.

That the use of symmetrical components takes the problem out of the restricted realm of tensor analysis into the more general one of spinor analysis, as stated by Dr. Hoffmann, is indicated also in the introductory equations 1-4 by the use of barred indices.*

The question whether the two steps *A* and *B* can or cannot be replaced by one spinor transformation must be left open. I have tried repeatedly to discover such a single transformation, but did not succeed.

There seems to be no agreement as yet among authoritative mathematicians whether the word "tensor" should include physical (or geometrical) entities that are functions of both real and complex variables or whether it should be restricted to functions of real variables, while the word "spinor" is introduced to denote physical entities that are functions of complex variables. Personally, I prefer to use the expression "tensor" for all physical entities.

That $\boldsymbol{R}$ and $\boldsymbol{L}$ both must be of special form in order that the method of symmetrical components should reduce them to symmetrical form is, of course, well-known. There is a real need in electrical engineering for transformations that reduce more general types of tensors to symmetrical form, and, if possible, to diagonal form. So far Fortescue's method is the only workable tool available. Professor Hoffmann, because of his immense background of transformation theory, may be of help to electrical engineers along these lines of development.

He correctly points out that $\boldsymbol{R}$ must be a multiple of the unit matrix along the reference axes, in which the equivalent circuit is to be established, if it is to be brought to a symmetrical form. That condition is not satisfied with unbalanced load on the rotor even for the reference axes under discussion, and $\boldsymbol{Z}$ then becomes hopelessly asymmetrical.

However, it must be emphasized that it is not at all necessary that $\boldsymbol{Z}$ should be expressible by a symmetrical matrix in order to represent $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ by a stationary network. The only necessary (though not sufficient) condition that $\boldsymbol{Z}$ should be representable by a stationary network, is that $\boldsymbol{Z}$ should be a tensor (or rather a spinor). The condition of symmetry only enters if the requirement is to use a mesh network. But if one cares to use an orthogonal network (a network with both impressed voltages and currents) the symmetry of $\boldsymbol{Z}$ is not a necessary condition.

As an example, let the steady-state equivalent network of a salient-pole synchronous machine Figure 2a of the paper be considered reproduced in the enclosed Figure 1A. The original equation $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ in which $\boldsymbol{Z}$ is not symmetrical has been given in reference 5, equation 14 (dividing row $\boldsymbol{f}_a$ by v and $\boldsymbol{b}_a$ by $-v$) as

$$\boldsymbol{e}=\begin{array}{c|c|c|}
\boldsymbol{d}_f & \boldsymbol{f}_a & \boldsymbol{b}_a \\
\hline
e_{df} & 0 & 0 \\
\hline
\end{array}$$

$$\boldsymbol{Z}=\begin{array}{c|c|c|c|}
 & \boldsymbol{d}_f & \boldsymbol{f}_a & \boldsymbol{b}_a \\
\hline
\boldsymbol{d}_f & 4r_f & 0 & 0 \\
\frac{1}{4}\boldsymbol{f}_a & 2jX_{ad} & \frac{2r}{v}+j(X_d+X_q) & j(X_d-X_q) \\
\boldsymbol{b}_a & 2jX_{ad} & j(X_d-X_q) & -\frac{2r}{v}+j(X_d+X_q) \\
\hline
\end{array}$$

Even though this matrix with three rows and two columns is not symmetrical, it can be represented by the equivalent circuit of Figure 1B containing three meshes in which an impressed field current i^{df} also exists. That current remains constant while the speed v changes.

The disadvantage of the circuit of Figure 1B is that on the network analyzer, the generator supplying i^{df} has to be adjusted each time v varies, while on the circuit of Figure 1A, all generators remain unchanged as v varies. On the other hand Figure 1B with its constant field current corresponds more nearly to the physics of the actual machine. In fact, it gives the resultant flux E_{df} linking the field winding, that could not be represented on Figure 1A.

In the problem of connecting a synchronous machine to a transmission line $\boldsymbol{Z}$ is asymmetrical and E_{df} has to be known. Hence the circuit of Figure 1B must be employed then and not the simplified form Figure 1A, in which the row and column of $\boldsymbol{d}_f$ (and the corresponding mesh) had been eliminated.

Temperature and Electric Stress in Impregnated-Paper Insulation

Discussion and author's closure of paper 42-10 by J. B. Whitehead and W. H. MacWilliams, Jr., presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 10–13.

L. J. Berberich (Westinghouse Research Laboratories, East Pittsburgh, Pa.): Doctors Whitehead and MacWilliams have shown that oil-impregnated paper subjected to high stress and temperature undergoes large increases in power factor in a relatively short time. They offer the explanation that this behavior may be caused by electrolytic dissociation of the oil and the effects of temperature and stress in accelerating this dissociation.

This problem can be viewed somewhat more broadly when the behavior of oil-impregnated paper is contrasted to that of the modern capacitor dielectric which consists of paper impregnated with chlorinated diphenyl. It is common practice to operate capacitors at voltage stresses in the neighborhood of 400 volts per mil and temperatures only a little lower than the 80 degrees centigrade used by the authors are reached in service. Little or no increase in power factor over long periods of time is observed under these conditions, which appear to be disastrous for oil-impregnated paper.

What is the explanation for this difference in the observed behavior of the two dielectrics? As for the synthetic impregnant used in capacitors, it has a greater residual electrolytic dissociation than oil which is reflected in a greater ion content and conductivity. This dissociation is probably equally well accelerated by voltage stress and temperature. The answer may lie partly in the fact that the dielectric constant of the synthetic material is greater than that of oil, and consequently there is less stress on the liquid component in the case of the capacitor dielectric for the same over-all voltage stress. The rest of the answer may possibly lie in the fact that there is some evidence that the synthetic liquid has a greater affinity for paper than oil because of its polar nature. Both the higher dielectric constant and the greater interfacial tension between the synthetic liquid and paper should make the formation of gas pockets less likely than in the case of oil-impregnated paper.

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* See reference 7 of the paper, May 1935, page 239, where the method of symmetrical components has already been presented as a full-fledged spinor transformation.

At this point I should like to ask the authors if they have any definite evidence which would exclude the formation of gas pockets and the ensuing gaseous ionization as another possible cause of the behavior they observed? This evidence may be difficult to obtain, because even if dry spots exist in the paper while stress is being applied, reimpregnation may take place before they can be found by dissecting the sample. However, if the test is run for a long time, sufficient X wax should be formed to become easily visible. Assuming that gas pockets can form in some way, explanation of the observed behavior on the basis of gaseous ionization is easy. It is known[1,2] that gaseous ionization can cause a rapid increase in ion content of the oil which is reflected in an increase in conductivity and power factor.

Even assuming that gaseous ionization may be a possible cause of the power-factor increase, there still remains to be explained why the effect is less at lower temperatures. This may be simply a matter of greater viscosity of oil, making it more difficult for the oil movement to take place which is necessary for void formation. It would be particularly interesting to repeat this work with a lower viscosity oil which should answer some of the questions raised. It should also throw further light on the mechanism proposed by the authors.

Figure 9 of the paper shows a very large accelerating effect of temperature on the power factor increase. I should like to ask if the power-factor data for the various points were all determined at the same temperature?

REFERENCES

1. INFLUENCE OF GASEOUS ELECTRIC DISCHARGE ON HYDROCARBON OILS, L. J. Berberich. *Industrial and Engineering Chemistry*, volume 30, 1938, page 280.

2. CORONA DISCHARGE ON LIQUID DIELECTRICS, J. Sticher, J. D. Piper. *Industrial and Engineering Chemistry*, volume 33, 1941, page 1567.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): At the beginning of the third paragraph of this paper it is stated that "Little attention has been paid to the study of the long-time effect of combined high temperature and high stress." Then the paper proceeds to give the results of tests of durations up to about 100 hours with stresses of 400 volts per mil on the impregnated-paper insulation.

In Figure 4 of my 1939 AIEE paper, "Load Ratings of Cable,"[1] are given power factor data obtained in a series of tests on experimental high-voltage oil-filled 132-kv cable installed in conduit. The maximum stresses on this cable ranged from 220 to 259 volts per mil, which are somewhat above the highest stresses on commercial oil-filled cable in this country. The duration of the test was several years, as indicated in the 1939 paper, and the maximum total duration of voltage application to the termination of the tests was over 60,000 hours. The copper temperatures were 80 to 95 degrees centigrade average for the 1,000-foot lengths for total periods of 9 and 24 months on two samples, respectively, and in some portions of the cables the copper temperatures were 120 and even as high as 140 degrees centigrade, in contrast with temperatures of up to 80 degrees centigrade in the paper under discussion.

In addition, we have been conducting, in our high-voltage laboratory, tests of oil-filled cable with maximum stresses of 294 volts per mil and with heating up to 100 degrees centigrade. The tests have been going for many months, and only small changes in power factor have occurred.

On the other hand, the Whitehead and MacWilliams paper gives data showing a very high rate of increase in the power factor (nearly 0.04 per week) for insulation subjected to 80 degrees centigrade and voltage stress of 400 volts per mil. Then, after giving other data, it is stated in conclusion 1 that deterioration of impregnated-paper insulation due to temperature and stress may begin earlier and increase more rapidly than commonly supposed. An exhaustive study of stresses of impregnated-paper insulation in operation, not only of cable but of capacitors, together with a study of the enormous amount of data obtained with experimental cable, indicates definitely that for the temperatures and electric stresses in effect in normal operation of impregnated-paper insulation, deterioration and chemical instability do not start anything like as early as indicated in the article. Perhaps there is some phenomenon that is peculiar to higher stressing, that is, at about 400 volts per mil, such as was used by the authors.

Long-time tests of oil-filled cables in Chicago showed little effect of superimposed voltage during heating periods upon changes in power factor as compared with similar periods of heating without voltage. In fact, it was found that application of voltage produced a decrease in power factor for hours or even days, while removal of voltage caused corresponding increases. This finding appears to be in contrast with the conclusions from test 3. However, Figure 7 of the paper seems to indicate further increase with power factor after removal of the voltage. We tried to explain the phenomena found in Chicago by assuming that application of voltage causes a shift in the balance of oxidation or deterioration products. Further careful studies of these phenomena should give valuable information on the mechanism of deterioration of insulation.

The conclusion is drawn from tests 6 and 7 that oxidation plays a relatively small role in the deterioration process. This conclusion is in contrast with the results of studies of oil-filled cables in Chicago and with the results obtained in the research project on insulating oils and cable saturants at the Massachusetts Institute of Technology under the auspices of The Engineering Foundation and AIEE. It should be noted, however, that the reported amount of oxygen of 0.7 per cent by volume is small, and the length of the test of about five days is very short. In aging tests of oil-filled cable in Chicago it took for one sample about one year and for another more than three years before considerable increases in power factor developed due to presence of some oxygen.

From the results of power-factor measurements of the individual layers, the conclusion is drawn that there was no chemical action from the electrodes. These tests were made at room temperature. Studies in Chicago showed that radial power-factor curves at room temperature show very little variations, while at 60 degrees centigrade they may show significant changes.

In order to obtain a better understanding of the processes involved in the power-factor changes observed, careful studies should be made of the effect of such variables as electrode materials, purity of the paper, oxygen content, and purity of the oil.

REFERENCE

1. LOAD RATINGS OF CABLE, **Herman Halperin.** AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–53.

Robert J. Wiseman (Okonite–Callender Cable Company, Inc., Passaic, N. J.): I have read with considerable interest the paper by Dr. Whitehead and Dr. MacWilliams. They show that when their samples of impregnated paper, without oxygen or electric stress present, are heated for a week at 80 degrees centigrade, practically no change in power factor results; when tested without oxygen and with stress, there is a change in ionization with temperature and stress; and with oxygen and stress present, about the same results are obtained. These results are of interest, but it would have been helpful if the authors had been able to continue their time of tests for many weeks instead of one week. Each of us has our particular method of testing impregnated paper for its power-factor stability, and we find it is possible for the first few days to show a decrease in power factor and then a slow steady increase or even a very large increase, depending upon the type of oil being tested and the kind of metal used as electrodes. Perhaps at some time Dr. Whitehead will repeat his tests, varying the oil and kind of electrodes with samples under a high temperature for two to three months.

I like the explanation offered by the authors for the increase in power factor with stress, namely, possibly due to electrolytic dissociation; yet I wonder if it is actually dissociation—that is, is it a permanent change in the oil? We have made tests on samples of impregnated paper which we are quite certain permitted no chance of gaseous ionization to take place, and yet we did find a change in power factor with stress. It has been customary to assume that when there is a change in power factor, voids are present and gaseous ionization takes place. Dr. Berberich has referred to this condition in his discussion of the paper. I do not believe Dr. Whitehead's samples had gaseous ionization. Where we believe that there is no chance of gaseous ionization, and we do find a change in power factor for want of a better term of what happens, we call it "electrolytic ionization." The change in power factor is usually very much lower than takes place when gaseous ionization takes place. As we view it, it takes place due to the ionization by impact of free ions as the electric stress is increased, if one will accept the assumption that there are free ions present in oil. Perhaps Dr. Whitehead's electrolytic dissociation and our electrolytic ionization are synonymous.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.: During one period we tested a large number of impregnated-paper condensers in communication with the body of oil used for saturation of the paper. Like the condensers of the authors, these were subjected to voltage stress at an elevated temperature. We finally concluded that our tests were seriously affected by diffusion of oil between the condensers and the surrounding oil body which was freely exposed to oxidation and absorption of oxygen from the air. Altogether different results are obtained on condensers (such as a cable) when the dielectric is completely sealed from any communication with the atmosphere. I believe the authors should give careful thought as to the possibility that their results may also have been influenced by interdiffusion of oil between the saturant of the paper and the surrounding oil body.

Hubert H. Race (General Electric Company, Schenectady, N. Y.): The conclusions drawn from the data presented in this paper are quite contrary to information obtained either from service or other experiments on miniature cables. In my opinion, the reason is that the authors failed to recognize that when specimens are tested at 80 degrees centigrade in an open oil bath, oxygen is present in sufficient quantity to produce oxidation products in sufficient concentration to account for the results obtained.

At high temperature in an open bath the rate of transfer of oxygen from the air to the specimen is increased greatly because of convection currents set up in the oil bath. No doubt the specimens were prepared substantially oxygen-free but as soon as they were opened to air and submerged in an open bath of oil they started to absorb oxygen. Oxidation products of both the oil and the paper would contribute to increased power factor.

For this reason I believe oxidation was the cause of the results presented in Figures 1–5 which were marked "no oxygen" just as much as for the subsequent experiments in which a limited amount of oxygen was purposely admitted at the beginning of the experiments. For both types of experiments the observed time rate of increase in power factor probably is determined largely by the rate of diffusion of oxygen into the specimen. This would account for the fact that the authors found little difference between the specimens which contained no oxygen at the beginning and those to which oxygen was admitted before the heating and measurements started.

In Figure 9 the authors state that *in the absence of oxygen* they have found a rate of increase of power factor at 80 degrees centigrade of 0.02 per cent per hour or about 0.5 per cent per day. At the 1939 summer convention I presented a paper[1] summarizing life tests on oxygen-free miniature oil-impregnated-paper specimens *assembled, evacuated, impregnated, and tested in glass* in which the five specimens having longest life had been tested at high voltage between 400 and 800 days before failure. Up to July 1937 these specimens were heated to 60 degrees centigrade for about 20 hours each day and afterwards to 100 degrees centigrade. After dissembly, the oil-soaked paper taken more than 1 inch from the breakdown path in these specimens had a power factor of only about one per cent. Furthermore, many specimens had practically constant power factor throughout their life. These data are not consistent with those presented by Whitehead and MacWilliams. The differences are reconciled, however, if it is understood that continuous oxidation was possible in their specimens.

In the paper to which I refer we point out unexpected metal migration and concentration in the region of highest stress adjacent to the electrode surfaces. My explanation has been that under conditions of *high electrical stress* and elevated temperature, chemical reactions occur which would not otherwise be possible. Therefore, I am in complete agreement with Whitehead and MacWilliams in believing that the electrical stress is a very important factor in deterioration resulting from heating and oxidation.

Reference

1. Tests on Oil-Impregnated Paper—IV, Mechanism of Breakdown, Hubert H. Race. AIEE Transactions, volume 59, 1940, pages 721–9.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): It is well-known from accelerated aging tests of cables that electric stress causes a deterioration power factor at a rate which increases rapidly with rise of temperature above 60 degrees centigrade, but these tests do not indicate rates even approaching those shown by the authors' tests. It has generally been assumed that this action has been due entirely to the increase of ionization caused by vacuums resulting from oil contraction after thermal expansion. That this is the main reason for the temperature effect is obvious from the fact that when precautions are taken to prevent voids, the deterioration of power factor is practically stopped at temperatures even higher than that used by the authors. The authors claim an effect apart from and additional to that caused by ionization of voids.

As load cycle tests are usually conducted at stresses of one half, or less, of the stress employed by the authors, the phenomenon they report must be peculiar either to the high stresses used or to their test method.

I cannot dismiss the test method, because I note from the characteristics of the oil used to impregnate the samples that, unless there is an error in the viscosity figures, the oil is of naphthenic base, and therefore while its power factor is not greatly susceptible to change by oxidation under the given test conditions, its power-factor stability against electrical discharges would be greatly impaired by the presence of oxygen. In view of the nature of the oil, I do not consider that the open tank method of test used by the authors is a satisfactory guarantee against *electrical instability* due to oxygen.

We have tested a well-known naphthenic base cable oil in the electric discharge cell described on page 209 of AIEE Transactions, volume 60, 1941, and have found the results shown in Table I.

This may be the key to the authors' data.

It must be remembered that a paper-insulated cable is a completely enclosed device and that it is unsafe to draw conclusions about cable behavior especially in relation to power factor from tests on unenclosed specimens.

At Mr. Halperin's suggestion I made some load cycle tests with heat periods lasting over days instead of hours and found some increase in power factor during the 80 and 115 degree centigrade test periods, but the magnitude was not of the order found by the authors.

The other alternative is that something happens at stresses of 400 volts per meter that does not happen at 200 volts per meter, that is something different in kind, rather than in degree. The authors suggest that this may be "electrolytic dissociation." Undoubtedly a number of free ions dissociated from impurities in the oil, if not from the oil itself, will increase with increasing stress, because ions of opposite sign tend to segregate and so reduce the rate of recombination without reducing the creation of new ions.

Table I

Condition of Oil	Power Factor at 100 C Before Discharge Cell Test	Power Factor at 100 C After 1,000-Minute Discharge Cell Test at Room Temperature
New	0.86	2.2
Partially oxidized	5.00	Unmeasurably high at or over 80 C

It is also true that heat will further this action by agitation, but I can see nothing new happening at 400 volts per meter which does not happen although in less degree at 200 volts per meter, nor can I see why the agitating effect of 80 degrees centigrade should be vastly greater than that at 60 degrees centigrade, a difference of only six per cent in absolute temperature.

K. W. Wagner's AIEE paper of 1922[1] shows characteristic curves of impregnated-paper insulation in which, at high current densities the stress actually decreases with increasing current density, or to put it another way, the current density increases even with decreasing stress. Does this condition indicate a combined electrical and thermal instability, under extreme conditions, such as the authors assume?

I do not believe that the authors have given us sufficient data to permit us to give a final evaluation of their work.

The series of papers on impregnated-paper insulation which Dr. Whitehead and his associates have brought out over a number of years should be regarded as parts of a unit which is not yet complete. I hope that the experiments which are described in the present paper will be amplified in the light of the discussions offered, for some future number of the series.

Reference

1. Physical Nature of the Electrical Breakdown of Solid Dielectrics, K. W. Wagner. AIEE Transactions, volume 41, 1922, pages 288–98.

J. B. Whitehead: The results reported in the paper have been criticized as showing a temperature plus stress increase of the power factor of impregnated paper much higher than that found in load cycles on impregnated-paper power cables, and also in laboratory tests. The most important points made are:

(*a*) The relatively small values of power-factor increase found in long load-cycle tests on cables.

(*b*) That the results found by the authors are probably due to oil oxidation, the oxygen entering the sample by diffusion, since the tests are conducted under an oil bath with air at atmospheric pressure above it.

The principal data on (*a*) are those given by Mr. Halperin from the well-known and valuable cable stability tests conducted by his company. The particular results he cites are the tests on oil-filled cables; and from a large array of data on such cables he has selected a few in which the increase in power factor in long load-cycle tests is relatively small. In response to this it should be pointed out that from the same data there are a number of cases in which substantial values of power factor increase are evident. The text of the paper itself points out a number of such cases, and, in particular, Figure 1 of Mr. Halperin's paper shows results on a solid cable in which there is an excessive rise of power factor due to combined temperature and stress. Moreover, in many of the load-cycle tests the temperature cycle is 24 hours, which is scarcely long enough for the detection of power-factor increases such as we have reported.

Load-cycle tests on finished cables such as those of Mr. Halperin are not of the best type for investigating the combined influence of temperature and voltage. Tests on finished cable and under the usual load-cycle programs introduce many variables other than those here in question. It is quite probable that the oil-filled cables referred to by Mr. Halperin have good temperature-voltage stability, due either to the properties of the particular oil they contain, or perhaps due to the oil-filled type of construction. It should be noted that our own tests were made with a solid cable oil. Incidentally, the increases reported by C. F. Proos, referred to in our paper, were found in load-cycle tests on oil-filled cables, and the author emphasizes the noticeable effect of combined high stress and high temperature, an effect which is absent under the same stress at lower temperatures.

As to (*b*), the suggestion that our results are due to excessive oxidation, the principal comment is that by Dr. Race who suggests that within the six-day period of our test, enough oxygen will diffuse into our 0.04-inch thick sample to account largely for the rapid rise in power factor we have reported, as an oxidation process. In reporting our results we have assumed that a comparison of behavior of two samples, one practically entirely free of oxygen, and one containing over 500 times as much oxygen, would be sufficient to test a possible influence of oxygen on our results. In the latter case, the results did indeed show a substantially higher rate of increase due to high temperature alone, but not by any means comparable in amount to the increase of power factor found in both these samples when under combined influence of temperature and stress. However, Dr. Race goes further and states that it is possible that the amount of oxygen absorbed in the test in an open bath is so great that it completely masks the difference observed in our two samples, and that both samples are substantially the same in the matter of oxygen content.

We do not concur with Dr. Race in this view, and fortunately we have available the results reported by T. B. Jones, AIEE Transactions, volume 56, 1937, page 1492) who, working with the same apparatus and a similar oil measured the time increase of power factor of impregnated paper for samples in which the oil had been saturated with oxygen at pressures up to 76 centimeters of mercury, and temperature up to 80 degrees, saturation having taken place before admission to the sample, and saturation of the paper taking place at the same temperature in an atmosphere of oxygen. The results of Jones show that the rise of power factor due to the absorption of oxygen from the bath open to the air is of the order of up to 0.01, that is, little greater than those reported in our paper for the oxygen sample, and the value observed at the end of one day in our combined voltage-temperature test. These values are on the assumption that the sample has become completely saturated with air from the bath, which fact we seriously question. It is also to be noted that in Jones' experiments on samples containing different amounts of oxygen, the measuring cell was evacuated immediately after each power-factor measurement and therefore remained throughout the test at the particular pressure corresponding to the amount of contained oxygen, except for the brief intervals in which power factor was measured.

Incidentally I may also mention that we have allowed cylindrical impregnated-paper specimens such as used in our accelerated life tests, to stand for weeks at a time submerged in an open bath, and have found no material change in the value of power factor measured immediately after impregnation.

Most important in the present work, however, is the difference in behavior we have found when the specimen is subjected to stress and when it is not. If our power-factor increases are due to oxidation alone, they should certainly show up in the absence of stress, and this they do not do. Is it possible that Dr. Race has overlooked this important fact?

With reference to Dr. Wiseman's preference for the term "electrolytic ionization," no serious objection can be taken to it. The word "ionization" has been overworked to such an extent that it applies to almost any unfavorable phenomenon in a cable. The word "ionization" arose first, I believe, from the phenomena in gases, and it was therefore natural that the term should be introduced to describe the phenomena going on in gas spaces in cables. However, it has always seemed to me undesirable to use this word when describing the behavior of liquids and solids, particularly those in which there is no free gas. Moreover, "dissociation" is a well-known term applying to all electrolytic solutions, and many authors consider that insulating liquids including oils have an inherent electrolytic dissociation. Dissociation in this sense, of course, means the continuous process of the separation of molecules into ions and the recombination of the latter, there being, however, on the average, always a certain number of free electrolytic ions. It is this type of ion that we have invoked in our suggested explanation of the phenomena reported in our paper.

We are glad to note that there is general admission that there may be conditions under which combined temperature and stress may lead to unlooked for increases in power factor. We heartily agree in the general comment that the matter should be investigated further with a variation of materials and other obvious variables. We recognize freely that we have tested only one oil and one paper, but we emphasize again that if *any* insulating oil presents the behavior such as we have noticed, attention should be called to it.

Field Investigation of the Characteristics of Lightning Currents Discharged by Arresters

Discussion of paper 42-17 by I. W. Gross, G. D. McCann, and Edward Beck, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 266–71.

E. R. Whitehead (Duquesne Light Company, Pittsburgh, Pa.): This paper is another valuable contribution aimed at rounding out our knowledge of lightning and protective systems by filling in a vital gap concerning the wave shape of natural surge currents.

The difficulty of obtaining such data has been overcome by persistence and resourcefulness, and if we add patience we shall in time accumulate a firm foundation for sound design and application of lightning protective devices.

The low magnitudes and slow rates of rise so far measured explain the highly satisfactory over-all performance of modern lightning arresters. There is, nevertheless, a number of instances in which failure to provide protection demands currents whose rate of rise and magnitude greatly exceed those indicated. None of us is satisfied with such inferences, however, and it is to be hoped that some light will be thrown on this question by the authors in the course of continued investigations. It should be realized that direct strokes quite close to stations are required to produce such currents, and for this reason these data may not be secured for some time.

In many instances it is possible to plan economical protection for discharges of this character and if application engineers give this problem appreciative attention, we may expect protection methods to keep pace with fundamental research.

W. J. Rudge (General Electric Company, Pittsfield, Mass.): The data contained in this paper are a valuable addition to the basic information essential to the economic design and application of protective devices. It is hoped that the authors will continue their investigation in the future.

In Figures 3 and 9 of the paper, the authors compare the results obtained with those obtained by Gross and McMorris. It is pointed out that the difference in the two results may be due to the difference in instrument sensitivity. This is apparently true as the surge-crest-ammeter links used by Gross and McMorris were set to have a threshold of measurement at 300 to 400 amperes; therefore, discharge current below this value would not be recorded. In Figure 9 of the paper by Gross, McCann, and Beck, it appears that currents as low as 100 amperes or perhaps less were recorded. Since low currents occur more frequently than high currents, it is natural to expect that with a lower threshold of measurement a larger number of discharges will be obtained.

It is of particular interest to note that the highest current measured in this investigation is 9,600 amperes, although five of the installations described in Table I were on wood-pole construction with no overhead ground wires where one would expect the highest discharge currents to be measured. The highest discharge current recorded by Gross and McMorris through a single arrester pole was 15,000 amperes with a probable maximum of 18,000 amperes (if the currents were added in two arrester poles on the same phase). In view of these results, it appears that the lightning arrester subcommittee reports, publishing the performance characteristics of arresters available to the industry up to discharge currents of 20,000 amperes, cover the range of lightning currents most frequently encountered on high-voltage transmission systems.

H. A. P. Langstaff (West Penn Power Company, Pittsburgh, Pa.): The West Penn Power Company has had the equivalent of five to six years experience with fulchronograph installations on our 25-kv system, the first of three being installed August 1939. A number of records have been obtained, one of which I understand is the maximum rate of rise obtained to date: namely, 7,800 amperes per microsecond. From a total of 40 surge recorders on this same system during this past year we have obtained 11 records varying from 4,500 to 10,000 amperes; nine between 10 and 20,000 amperes; one between 20 and 30,000 amperes; three between 30 and 40,000 amperes, and one over 40,000. All arresters involved were of the line type, and a number of them were of the 20-kv rating. The one discharge over 40,000 amperes destroyed the 25-kv arrester. One 20–30,000-ampere discharge was through a 20-kv arrester with no resulting damage. We are continuing our application of 20-kv arresters on the 25-kv system with desirable economical results, and most all of the line-type arresters are mounted on suspension, thereby allowing all damaged arresters to clear the circuit and allow service to be re-established.

Loss-of-Field Protection for Generators

Discussion and author's closure of paper 42-20 by G. C. Crossman, H. F. Lindemuth, and R. L. Webb, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 261–6.

S. B. Griscom (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper brings out an interesting point in connection with the stability of metropolitan power systems. On transmission systems when stability is a serious problem, it is usually due to such factors as large initial phase-angle displacements, low inertia factors, reduction of synchronizing power by switching out lines, and large reductions of generator output by line short-circuits. These considerations are practically never a factor in the stability of metropolitan systems because of the close proximity of generating capacity and loads, the multiplicity of transmission circuits, and the inherently better stability characteristics of steam turbine generators compared with water-wheel generators.

As may be expected, general instability of metropolitan power systems is very rare, possibly occurring at intervals of from 5 to 20 years on any given system. When instability does occur, it is more apt to be caused by some major apparatus failure, or to an operating error. In other words, with metropolitan systems, the problem is not improvement of stability, but the elimination or neutralization of incidents which might cause instability. The most frequent causes of instability of metropolitan systems have been prolonged short circuits at points of high-power concentration and loss of generator excitation. Recent advances in bus differential and backup protection systems have largely solved the short-circuit problem, and the loss-of-field protection system described by the authors appears to satisfactorily solve the excitation problem.

The combination of an undervoltage relay with the reactive current relay is of particular interest in that it gives double assurance against false tripping. In the minds of most operators, this requirement is probably paramount.

Harold A. Bauman (Brooklyn Edison Company, Brooklyn, N. Y.): Those of us who are operators of generating stations are primarily concerned with improving the reliability of our service by reducing the possibility of serious system disturbances.

These loss-of-field protection relays were welcomed by all of our operators as filling a gap in our generator protection. The first installation, at Hudson Avenue station of the Brooklyn Edison Company, proved the extreme reliability of this equipment. The only maintenance required has been the half yearly operation check.

Tests made by actually tripping out the field of one of our generators while on the bus carrying load proved that the protective relays functioned so well that even the high board operators could hardly observe any disturbance on their station meters.

We are confident that this new loss-of-field protection will prevent serious system disturbances and that a long wanted need has been filled in our system protection.

Earle Wild (Commonwealth Edison Company, Chicago, Ill.): The use of this relay for locating complete or partial loss of excitation on a turbogenerator should be very valuable, as this is a rather rare and often a difficult type of trouble for an operator to analyze. This relay would remove any doubt in the operator's mind as to the cause of the severe oscillations on his generator. It should be pointed out, though, that it is neither desirable or necessary in the metropolitan operating company to remove the generator from the system. The ability of nonsalient pole turbogenerators to resynchronize themselves is very pronounced and the correct procedure would be for the relay to indicate by alarm the nature of the trouble. By either reducing the steam input or restoring the field, the generator would very quickly come back to synchronous speed. In the short interim the speed has been increased considerably to the slip speed of an inductive generator. On large systems the reactive requirements of an induction generator are met with only a slight drop in voltage for the few seconds before an operator could act.

This conclusion is based on the analysis of approximately 18 cases of trouble and tests on the Chicago system which were made over ten years ago and reported in a paper in the December 1931 TRANSACTIONS of the AIEE.

E. E. George (Ebasco Services, Inc., New York, N. Y.): The scheme of relay protection outlined in this paper seems to provide satisfactorily for a reasonable range of operating conditions and to be adaptable in principle to different sizes and types of units.

Apparently, the scheme always operates to disconnect the generator if the field is lost or is reduced so low that predetermined conditions of voltage, current, and phase angle prevail. It is not clear whether any consideration has been given to reducing the turbine throttle opening so as to lower the generator kilowatt output and prevent instability in those cases where no hazard to equipment is involved in continued operation.

On hydroelectric machines it has been found possible to clear up an out-of-step condition in which the generator was slipping poles rapidly by merely reducing the turbine gate opening. This procedure was satisfactory even in the case where the field was completely removed from the generator, and it permitted the field to be restored without ever opening the generator oil circuit breaker.

There would seem to be cases in some systems where reduction in generator kilowatt loading might be preferred to opening the generator oil circuit breaker. This provides further argument for the idea of position limit interlocking between the generator voltage regulator and the turbine throttle or gate opening.

G. C. Crossman: In reply to Mr. George's comments on reducing turbine throttle opening to lower generator kilowatt output and thus prevent instability during loss or material reduction of generator field excitation, the authors would like to point out that such throttle control has been considered. However, since the system with which the authors are connected is designed to stand the loss of capacity represented by the disconnection of any one unit in any station, it has been felt that the added complication of such control could not be justified, especially in view of the speed with which serious voltage reductions can occur with loss of field on this system.

Mr. Wild has indicated that on the Chicago system it has not been felt desirable or necessary to remove an unexcited generator from the system since only a slight voltage drop is experienced for the few seconds before the operator could act to reduce generator load or restore field. As indicated in the paper, the authors recognize that there are some systems where loss of field may occur without serious voltage disturbance and where corrective measures may be taken without disconnecting the generator from the system. However, studies and experience indicate that on the New York metropolitan system serious voltage disturbances can occur within a relatively short time, sometimes as short as one or two seconds. Furthermore, when such a disturbance does occur, and the unexcited machine loses synchronism, it is often very difficult for the station operator to determine which machine is in difficulty and what that difficulty is. Several years ago loss of field occurred on a 160-megawatt 25-cycle unit. A serious voltage disturbance resulted over the entire 25-cycle system, with considerable equipment damage. Due to the confusion produced by the fluctuations occurring in practically all the station instruments, considerable time elapsed before the operator was able to identify the machine in trouble and remove it from the system.

In connection with Mr. Wild's comments as to restoring field without disconnecting the generator from the system, it should be pointed out that probably the majority of excitation system troubles cannot be corrected without removing the machine from the system. Of the six cases of complete loss of field which have occurred on the New York system, only two resulted from opening of excitation system breakers, while the other four resulted from such various causes as: field rheostat control circuit trouble; pilot exciter rheostat open circuit due to over travel and contact trouble; and contact failure of a pilot exciter brush during replacement of the paralleling brush.

At the time the paper was written, there had been no operating experience with the loss-of-field relays described in the paper. Subsequently, however, on the evening of January 23, 1942, loss of field occurred on a 65-megawatt, 60-cycle, 3,600-rpm unit equipped with these relays. The unit was carrying a load of 60 megawatts. Through an operating error in shutting down another machine, the pilot exciter breaker on this machine was tripped, causing complete loss of field. The relays quickly disconnected the machine from the system without appreciable disturbance in bus voltage.

Linear Couplers for Bus Protection

Discussion and authors' closure of paper 42-40 by E. L. Harder, E. H. Klemmer, W. K. Sonnemann, and E. C. Wentz, presented at the AIEE winter convention, New York, N. Y., January 26-30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 241-8.

C. D. Hayward (General Electric Company, Philadelphia, Pa.): Bus protection continues to be a most popular subject at the protective-relay sessions. Fortunately for the relay designer, the attention of interested engineers continues to focus (and rightly so) on the current transformer rather than on the relay as the piece of equipment most susceptible to improvement.

The paper on transient characteristics of current transformers during faults by Messrs. Concordia, Weygandt, and Shott[1] today clearly demonstrates the impracticability of using standard current transformers for generator bus protection.

At the Toronto convention last summer Messrs. Kennedy and Sinks[2] showed that a nonsaturating transformer with linear characteristics was necessary, if adequate bus protection was to be provided. They chose the air-gap-core current transformer in preference to the nonmagnetic-core or linear coupler, as described by the present authors, because of the higher level of power which it affords to operate the relays.

The nonmagnetic-core transformer, or linear coupler, is by no means a new device. The interesting magnetic properties of toroidal coils which make them attractive for use as current transformers have been known for a long time. The mathematical demonstration of the complete freedom of the ideal toroidal coil from mutual coupling effects has been used as a textbook example in volumes on electricity and magnetism[3] for many years. It has long been common practice to use toroidal loading coils in telephone circuits to reduce inductive "cross talk."[4] The use of air-core current transformers for differential protection was covered by United States patents[5] issued ten years ago.

The chief stumbling block which discouraged the use of air-core transformers, or linear couplers, in the past was the low amount of power which could be obtained from them to operate the relays. This trouble has been overcome in the air-gap-core current transformer by making the air-gap portion of the core just long enough to prevent saturation of the remaining iron portion on the highest fault currents to be encountered, but no longer, so that the power delivered is not unnecessarily reduced. Since the maximum power which can be obtained from a given sized current transformer on a given primary current is proportional to the square of the effective permeance of its total core magnetic circuit, and since the air gaps in a typical air-gap-core current transformer constitute only about one sixth of the total magnetic circuit, it will be evident that a current transformer with an air-gap core can deliver about 36 times as much power under optimum conditions as it could if its core were all air.

The authors of this paper have apparently accepted the low power level of their linear couplers as inevitable and have been forced, therefore, to use delicate low-energy relays in order to obtain pickup on acceptably low values of internal fault current. The much higher power level afforded by the air-gap-core current transformer makes it possible to use standard-type sturdy reliable relays and to obtain operation on lower fault currents.

The air-gap-core transformer due to the greater power available, can also readily be used with standard types of relays having a small percentage of through-current restraint where minimum settings for internal faults are desirable, whereas the extra burden of restraint coils and connecting leads would probably be excessive for the linear coupler. With the air-gap current transformer only a small percentage of through-current restraint is required, just enough to overcome the tendency to operate on the small differential current due to the slight mismatching of the mutual reactances of the transformers. It is not necessary to have a separate restraint magnet on the relay for each bus circuit, as it is nearly always possible to group the circuits so that the three separate restraints available on standard relays are sufficient for busses having any number of circuits. With this arrangement the tendency of the relay to operate on heavy through faults is entirely eliminated, leaving the sensitivity of the relay as the only limitation on the minimum fault setting. Thus with standard relays and the air-gap-core transformer, the setting can be made low enough for any practical bus installation.

Since theoretically the linear coupler should be free from mutual coupling effects of external fields, it would be interesting to know whether the authors have any explanation to offer for the seemingly high error of 1.6 per cent given as due to astatic effects, especially when the air-gap current transformer without its shielding case was only one per cent and was too small to be accurately determined with the shielding case in place.

Also the linear coupler would appear to be easier to manufacture with close tolerances than the air-gap transformer; hence, we are disappointed to see the authors state that the couplers can be built with an accuracy of plus or minus 1.5 per cent which is no better than that given for the air-gap transformer at the summer convention.

Since no greater accuracy is claimed for the linear coupler, and since its power level is very much lower than that of the air-gap-core transformer with similar linear characteristics, it would appear that considerably more development would be necessary before the linear coupler can be accepted as a desirable transforming means for bus differential protection.

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3. For example see:
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4. THE PROPAGATION OF ELECTRIC CURRENTS IN TELEPHONE AND TELEGRAPH CONDUCTORS, J. A. Fleming. D. Van Nostrand Company, Inc., New York, N. Y., 1911. Page 273.

TELEPHONE COMMUNICATION, Wright and Puchstein. McGraw-Hill Publishing Company, Inc., New York, N. Y., 1925. Page 221.

5. United States patent 1,760,541, Bruckel. United States patent 1,766,048, Trogner.

G. Camilli (General Electric Company, Pittsfield, Mass.): The authors have described an interesting application of the linear couplers to the operation of differential relays for bus protection. Unfortunately, the output which can be derived from the toroids described in this paper is very limited and requires highly sensitive relays.

It might be of some interest to those not familiar with the art to know that linear couplers have been used in the past for the measurement of losses at low power factors (see: Method of Measuring Losses in Reactors at Low Power Factor, by K. G. R. Wilkinson & T. W. Carter), *BEAMA Journal*, May 1939, pages 145–50.

As in the paper under discussion, in this case also, the purpose of the toroids is to obtain a voltage which is directly proportional to the primary current.

The scheme of measuring losses is quite accurate, because practically no load is derived from the toroids. The voltage across their secondaries is balanced with the voltage obtained across the apparatus to be tested in a potentiometer network.

Careful adjustments and very accurate calibrations are necessary when couplers are used to supply even very limited loads.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): A bus-protection scheme, having the well-known advantages of differential protection and yet being free from transient effects, should be highly useful in overcoming the reluctance of many operators to install any bus-protective equipment. This reluctance is not too difficult to understand, in view of the fact that if one false operation of a bus-protective equipment occurs, perfect performance must then be secured for many years, if the scheme is to have as good an operating record as conventional transmission-line relays. Also the seriousness of an incorrect bus clearing is such that false operations are next to intolerable. Accordingly it is well to consider carefully whether a setting margin of six per cent is sufficient to take care of an expected manufacturing tolerance having a three per cent spread, assuming that there are no other possible sources of error.

In considering the desirable operating range of bus-protection relays, another factor in addition to the range of normal maximum and minimum connected generation is of importance. Particularly in the case of outdoor busses, it may be highly desirable after a bus-protection operation to make a brief inspection and then energize the bus from a single generator. The bus-protection relay should, of course, operate with positive margin for this condition.

The lower operating limit given by the authors for the low energy plunger relay is 1,500 amperes or about $7^{1}/_{2}$ volts. For a high voltage system (such as 138 kv.), this setting amounts to some 360,000 kva which may not be low enough. The sensitive polar-type relay apparently has an available setting of about one third of this value. What is the opinion of the authors on the necessity of any special precautions in control cable elements to prevent the possible induction of some two volts in the high impedance relay circuit?

Mention is made of adjusting the mutual reactance of the couplers to the correct value after they have been wound. Details of the adjusting method would be of interest.

E. L. Harder, E. H. Klemmer, W. K. Sonnemann, and E. C. Wentz: The discussion by Mr. Hayward indicates that he believes that the paper on "Transient Characteristics of Current Transformers During Faults," by Messrs. Concordia, Weygandt, and Shott, clearly demonstrates the impracticability of using standard current transformers for generator bus protection. The wave forms and error currents calculated in the paper referred to are quite interesting in that they do not appear to be too much out of line with what is to be expected. However, to interpret the results of that paper in such a way as to conclude that generator bus protection using standard current transformers is impracticable is entirely out of line. It has been clearly shown by calculation, test, and operating experience, that the principle of utilizing variable percentage characteristics in relay design, together with utilizing fault current for restraint, is and has been quite feasible. Furthermore, standard current transformers are used, selected so that they meet well-established criteria with respect to a-c performance which are neither difficult to specify nor hard to meet. It should be borne in mind that the whole story is not given when a study of current-transformer characteristics shows that a false differential current will flow during certain external fault conditions. The picture is only completed when a satisfactory relay design has been evolved to meet these conditions, or when the problem is given up as being hopeless. The situation, as it exists today, is that the limitations in current-transformer performance under transient conditions have been satisfactorily offset by adequate relay design.

Mr. Hayward puts emphasis on the fact that the toroidal coil is not new. The justification for his attention to this point is probably the fact that in our paper we stated that a new principle was applied to bus differential protection. What the authors meant, of course, was that while the toroidal coils in themselves are not new, the application to bus differential protection is new in that it has not been done before. The fact that the principles involved in the accurate performance of air-core toroidal coils are not new is a positive advantage in that it adds an unquestioned background of accuracy fully justifying the application of these devices.

The fact that a limited amount of energy is available from the air-core transformers or linear couplers has undoubtedly been a deterrent against previous investigation on a serious basis. However, close examination of the facts brings out the interesting point that the difficulty anticipated in the past was a great deal more fancied than real. To substantiate this point, let us consider the relative values of energy levels required to operate several standard relay designs as shown in Table I.

The standard-energy close–open relay operates at 17 volt-amperes at tap value, and this is a figure with which most of us are familiar. The plunger-type relay and the polar-element relays were the ones used in the tests described in this paper. Both of these elements are standard designs as are found in many Westinghouse relays. The figures given for the type *D*-2 relay are rather startling because of the low energy level indicated, but this is easily verified from published information. For example, the resistance of the moving coil is given as 0.213 ohm, and a contact setting requiring only one millivolt across the coil for operation is easily made. From these values of volts and ohms the figure of 0.0000047 volt-ampere results.

Reviewing the above, it is seen that practical relays operate over a range of energy requirements of 3,620,000 to 1, this range being obtained by dividing 17 by 0.0000047. The most sensitive relay used in our tests was the polar-element relay requiring 0.02 volt-ampere, and this is 4,250 times the amount of energy required by the type *D*-2 relay. With this tremendous range in relay energy requirements to pick from, it seems to the authors to be rather pointless to make a vigorous effort to fill a portion of the magnetic circuit of the coupling device with iron, particularly in view of Mr. Hayward's statement that the best that can be done is to obtain a 36-fold increase in power level. Furthermore, when iron forms any part of the magnetic circuit, it introduces its characteristics into that part of the magnetic circuit so that the d-c saturation problem is not at all voided. The parameters of the circuit are merely moved to higher current levels, which merely give the current-transformer designer a little more freedom in certain respects, but which introduce other complications. For example, a parallel circuit must be used, bringing in such complications as temperature errors inherent in the application of modified current transformers of this type and the necessity for a multiplicity of adjustable impedances for impedance-matching purposes.

Mr. Hayward indicates that with the air-gap-core transformer there is plenty of energy to energize restraining coils of the differential relay in those cases where it may be desirable. In this respect, there is no difference whatever between the two schemes. Restraint is necessary only when the accuracy of the device is not sufficiently high to cover the desired range between maximum external fault and minimum internal fault. Experimental work has been carried out to a sufficient extent to show that should it become desirable to add restraint to the linear-coupler relay, the linear couplers can furnish the energy required without difficulty.

Table I

	Volt-Amperes
Standard energy close–open relay	17
Plunger-type relay	0.5
Polar-element relay	0.02
Type *D*-2 relay	0.0000047

Attention is called to what Mr. Hayward feels is a seemingly high error of 1.6 per cent given in the paper. If the authors could have made this test at a total error of three per cent, it would have been done. Actually, the only way we could get the maximum error of 1.6 per cent was to deliberately adjust the position of the poorest coupler of the entire lot for maximum error. A total of three per cent error would have been used as stated above for this test, if a number of couplers just meeting the limits which we have reserved of ±1.5 per cent had been available. Actually, the only way to operate under the maximum error of three per cent, in practice, would be to have all of the couplers in the circuits carrying current into the bus in error a maximum amount in one direction, and the linear coupler in the circuit carrying the current away from the bus in error the maximum amount in the other direction. For example, if on a six-circuit bus the linear couplers in five source circuits were 1.5 per cent high in their response, and the remaining linear coupler in the faulted circuit was 1.5 per cent low in its response, then the maximum error of three per cent would be obtained. The probability of the linear couplers being so disposed is very remote; furthermore, not all of them will be in error on either side by the maximum of 1.5 per cent.

Perhaps experience will show that the authors have been unduly pessimistic in assuming that 1.5 per cent is a normal level of error to be experienced in economic manufacture of these devices. It is fully appreciated that similar devices are made much more accurately for use in precision testing; hence, they could be made just as accurately for bus differential protection. With such increased accuracy, however, the cost of the device would obviously be increased, and it was felt that at the present time the best compromise between high accuracy and reasonable cost would be met at the level of ±1.5 per cent error.

Mr. Camilli points out that "careful adjustments and very accurate calibrations are necessary when couplers are used to supply even very limited loads." In this case, the loading on the couplers is of minor consequence with regard to accuracy and comes into play only for the tripping operation on minimum internal fault. If the linear couplers are out of calibration by 1.5 per cent, then the minimum pickup point of the relay would be slightly affected. Actually, the variation between individual couplers as well as the fact that the relay will not be provided with an infinite number of taps for 100 per cent accurate impedance matching makes it obvious that slight errors in individual linear couplers are inconsequential with regard to the minimum pickup adjustment. A check between calculated pickup current and actual pickup current is given in the test results. What Mr. Camilli has undoubtedly overlooked is the fact that during external fault conditions when the relay must not operate, the linear couplers are performing effectively on open circuit, without loading, because their voltages are in opposition in a series circuit.

Mr. Brownlee wonders if a margin of six per cent is sufficient to take care of an expected manufacturing tolerance having a three per cent spread. The margin given is entirely adequate in that the three per cent spread made up of ±1.5 per cent errors includes all errors which may appear in the couplers, astatic or otherwise. In view of the considerations pointed out above, it is expected that the theoretical maximum error of three per cent will never occur, but even if it did, the safety factor of 2 to 1 would still obtain, which is adequate.

In Mr. Brownlee's second paragraph, our interpretation is that under certain conditions, a minimum of connected generation may exist which is lower than the usual or expected minimum. The problem is, then, to determine what is the real minimum in any application before determining the range between maximum and minimum fault currents.

With regard to the induction in the control cable, the relative disposition of the various parts of the circuit should be considered. In the linear coupler itself, a large number of turns surround an area of several square inches placed as favorably as possible with respect to a primary conductor for the induction of voltage. The control cable between linear couplers and the switchboard should consist of two wires in the same conduit or duct. This cable run will consist of only one turn with a minimum of area between the two wires permitting flux to link the circuit. As long as the two wires are run in the same duct or conduit, it is obvious that the problem is of minor consequence, particularly where the cable is in iron conduit. In one instance, for example, the pickup in the leads amounts to less than one per cent of the minimum voltage used for relaying. Where the exposure to induction is very severe, however, the solution is to use conductors in twisted pair in iron conduit.

In response to Mr. Brownlee's question about the method of adjusting the reactance, the more obvious way is to add or subtract turns until the proper reactance is obtained, and then rewind the last layer so that it is uniform around the circle. A better way more suited to production is to have a section of the winding which is uniformly wound for coarse adjustment, which can be connected in addition or opposition with smaller sections of the winding, also uniformly wound for finer adjustment with one turn taps in one of these sections for the very finest adjustment of all.

Current-Transformer Performance Based on Admittance-Vector Locus

Discussion and author's closure of paper 42-11 by A. C. Schwager, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 26-30.

E. C. Wentz (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): It is to be feared that the multiplicity of methods for calculating ratio and phase angle which have been presented during the past few years has served to confuse, rather than aid the application engineer, who should be the one particularly benefited. The design engineers are taking such a great interest in the problem because they hope that a single set of $F\cos\theta+M\sin\theta$ saturation curves or an admittance-vector-locus curve or a nomogram, or equivalent, will be accepted by the application engineer in lieu of a huge number of ratio and phase-angle curves. At one stroke the designer is saved a great deal of work, and the application engineer gets more fundamental information. He can get all the possible ratio and phase-angle data, if he is willing to make a few simple calculations. Yet it is doubtful that the great majority of relay application engineers have thus far been impressed.

It should help to correlate these schemes in the minds of application engineers if we sort out the points on which the designers agree, though they say different things, and discuss briefly the points on which they disagree, even though what they say sounds the same.

Those who start with Agnew all agree on fundamental principles, as Agnew and they evade the "internal reactance" or "leakage-flux" hurdle. Mr. Sinks[1] went back further than Agnew and showed conclusively that Agnew's simple vector diagram will not work if considerable leakage flux is present. However, the author of this discussion worked out a method[2] by which the Agnew formula can be applied, if one is willing to accept a definition of internal reactance in terms of a certain measured value of leakage flux. Yet at this time it is not possible to say that a single simple method for including leakage flux or internal reactance will soon be generally accepted as a standard method.

However, recognizing that leakage reactance may often be neglected, there is fundamental agreement among the designers. The obstacle to calculation is that Agnew's formula contains that pair of trigonometrical bugaboos:

$$F\cos\theta+M\sin\theta$$
$$M\cos\theta-F\sin\theta$$

and that these lead to too much arithmetic. "Simplification" of these bugaboos has been the object of Wiggins, Woods and Bottonari, Wentz, and finally Mr. Schwager. The number of these simplifications may have caused confusion, but it must be remembered that the solutions are all the same in the end; the only reason for preferring one method over the other is that one likes it better. (This is not quite true when Woods and Bottonari suggest that $F\cos\theta+M\sin\theta$ may be equal to $\sqrt{F^2+M^2}$ as this is simplification by sacrifice of accuracy.)

The two principal methods of accurate simplification are then Mr. Schwager's, which, in effect, plots $F\cos\theta+M\sin\theta$ and $M\cos\theta-F\sin\theta$ simultaneously as a vector locus, and the author's,[2] which plots them separately as a family of curves. In either type of curve volts or volts per turn can be plotted against admittance or admittance for one turn (Mr. Schwager's) or amperes or ampere turns (the author's). Both can be particular or general.

It is not possible to say that either method is better. Whatever method is easier to use is better; the decision is up to the application engineers. However, the benefits of general use of either one of the methods and accompanying data are so important to both design and application engineers that it is to be hoped that the application engineers will study both methods and endeavor to make a decision, perhaps through an appropriate AIEE committee, recommending one method and an accompanying standard form in which manufacturer's data are to be submitted.

REFERENCES

1. COMPUTATION OF ACCURACY OF CURRENT TRANSFORMERS, A. T. Sinks. AIEE TRANSACTIONS, volume 59, 1940, December section, pages 663–8.

2. A SINGLE METHOD FOR DETERMINATION OF RATIO ERROR AND PHASE ANGLE IN CURRENT TRANSFORMERS, E. C. Wentz. AIEE TRANSACTIONS, volume 60, 1941, October section, pages 949–54.

3. A PROPOSED METHOD FOR DETERMINATION OF CURRENT TRANSFORMER ERRORS, G. Camilli and R. L. Ten Broeck. AIEE TRANSACTIONS, volume 59, 1940, September section, pages 547–50.

P. O. Langguth (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. The author has presented a unique method of determining the performance of current transformers and represents a new viewpoint not previously considered for popular use. It is interesting to note that in the past several years, the diversity of problems in relaying has brought about a score of methods, all aimed toward the simplification of voluminous performance data, yet flexible enough to meet the demands of present-day relaying problems.

2. The paper very clearly illustrates the progressive steps normally considered by the designer and is of equal interest to the relaying engineer. Familiarity with the saturation characteristic of apparatus associated with magnetic devices, I believe, is universally understood by the designer and relay engineer alike. The direct values obtained from these data represent a popular yardstick for current transformers. The quantity of admittance as brought forth in the paper is also a useful measurement with perhaps greater appeal to the designing engineer.

3. The writer, however, believes that many characteristics are immediately apparent from the saturation curve based on induced voltage and excitation data. These data may easily be obtained from the completely assembled transformer and interpreted for many varying conditions such as diversity of burden, ratio, and current. Inspection of a saturation curve reveals the so-called linear range which may suffice for many ordinary relaying problems. In such cases, the marked ratio may be considered ideal over this range. A further extension of the curve beyond the so-called "knee" will provide the application engineer with sufficient data for most applications.

4. For greater accuracy, the vector conditions with relation to the transformer output and excitation components must be considered. Mr. Schwager's method has combined the effect of the exciting component by determination of "admittance-vector loci" and has the advantage that the path of this two-dimensional vector of excitation has been definitely established for a given transformer. It also reveals what proportion of ampere turns will finally be left for the output.

5. Considering the use of the saturation characteristic directly from test data, I would like to suggest here another method which is based directly on an earlier AIEE paper by Messrs. Woods and Bottonari. It consists essentially of the combination of the vector diagram and the saturation data into a functional diagram and has the advantage of being relatively a simple and direct graphical solution. Such a scheme is illustrated in Figures 1 and 2.

6. Basically, these charts represent a graphical solution of the transformer vector diagram. The variable element, for example, the excitation of the transformer is obtained from the saturation curve plotted in Figure 1. This is combined vectorially in Figure 2 with the secondary current under consideration to obtain the ratio and phase-angle error of the transformer. An example is worked out in Figure 2, and the graphical steps are indicated by the dotted arrow lines.

7. It is my feeling that this idea or a similar one could be standardized to the extent whereby the charts could be made available on standard AIEE forms. Figure 1, for instance, could be so prepared that the originator of the transformer would merely draw the two curves on the form. Figure 2 would be a standard graphical form for computing the ratio and phase-angle error directly from data obtained by use of Figure 1.

8. In conclusion, Mr. Schwager has presented an interesting method that reveals many functional details of a current transformer. He has demonstrated how a large amount of transformer information can be expressed in one chart as compared to the voluminous number of ratio and phase-angle curves normally experienced in practice today. Since this paper evidences a continuing interest in this general subject, I suggest that the relay subcommittee arrange to prepare some simplification of cur-

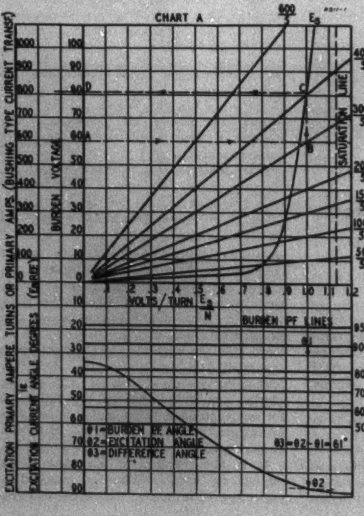


Figure 1

Figure 2. Ratio and phase-angle conversion

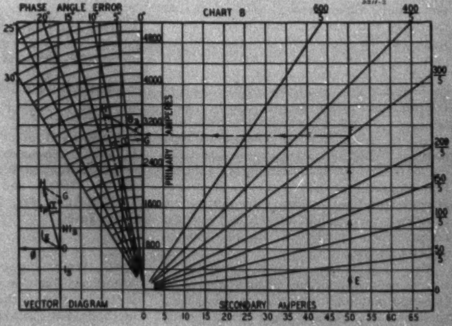
Example: Burden 1.2 ohms, 90 per cent power factor; 300 to 5 ratio; secondary amperes 50; bushing type

1. Burden volts = 50×1.2 = 60 volts

2. Chart A—For 60 burden volts (*A*) determine excitation current for 300 to 5 tap (*B*) reading saturation curve at *C*—810 amperes. Determine θ3 for an *Es* corresponding to point *B* for 90 per cent power factor *N* as indicated

3. Chart B—At 50 secondary amperes (*E*) determine ideal primary current for 300 to 5 tap (*F* and *G*). To this add excitation current (810 amperes) (*GH*) at an angle θ3. Polar co-ordinate of point *H* represents the correct primary current (*OH*) and phase-angle error α. Values are: 3,480 amperes and 12-degree phase angle

4. True ratio = 3,480/50 = 69.6



rent-transformer data and have its use adopted in standard form in the near future.

Otto A. Knopp (Pacific Gas and Electric Company, Emeryville, Calif.): Through his paper, "Current-Transformer Performance Based on Admittance-Vector Locus," Mr. Schwager has performed a valuable service in having perfected a method of presenting the characteristics of a current transformer in an exceedingly simple and ingenious way. Such a method was badly needed by the manufacturer of such transformers.

The writer, who has always had a great fascination for current transformers of all types, principally for methods of testing them and testing with them, has always felt as a utility engineer that the automatic calculation by test is the simpler procedure for determining the ratio and phase angle of a current transformer, rather than the calculation of these errors from the electric constants of the iron core by the methods originally developed by P. G. Agnew.

For manufacturers the calculating method has, no doubt, great advantages for selecting the size and type of core, ampere-turn value, size of conductors, and so forth. Mr. Schwager, working for a manufacturing concern, was, therefore, interested in this calculating method, and he succeeded in developing new and useful graphical methods which not only reduced the calculation work materially, but furnished an excellent method of presenting the basic characteristics of the current transformer from which all other performance data can now be quickly determined any time they are needed, without subjecting the current transformer to new tests. This is particularly useful when the individual transformer has been sold by the manufacturer and is no longer available to him for test.

The writer, working for a public utility, is naturally more interested in using direct test methods such as described in his AIEE paper, AIEE TRANSACTIONS, May 1936,[1] for error determinations, as the transformer to be tested is in his possession and remains in his possession. Therefore, a simple test under the existing conditions, is the best approach toward getting accurate results and avoids tedious calculation and the necessity of many tests for determining the necessary constants needed for the calculation. More time would be consumed in the determination of the value of the various burdens and their power factors than in determining the ratio error or phase-angle error directly, since most utility laboratories are well equipped for testing ratio and phase-angle error of circuit transformers. However, there may even be good use for Mr. Schwager's approach in the utilities, if the manufacturer who supplies the current transformers furnished their characteristics in the form Mr. Schwager suggests. The engineering department in the utility, confronted with the problem of selecting current transformers for certain relay operations, can select a current transformer more precisely by the Schwager method than by referring to a few specific ratio and phase-angle error curves which may not apply to the particular condition.

Another application would be the use of the "admittance-vector locus" for setting up current-transformer standards, replacing the *XYZ* standards or the new standards proposed: *B*–0.1, *B*–0.2, *B*–0.5, and so forth. Another, and, in the writer's opinion, the most useful application of the "admittance-vector locus," would be by its use in the reverse. It should not be difficult, with the accurate methods and equipment available in the utility laboratories for determining ratio and phase angle of current transformers for all kinds of burdens, to determine the "admittance-vector locus." Thus, it may be possible in the utility laboratory to determine this locus as a matter of record more accurately that way than it could be determined by making measurements of the iron loss and other electrical constants of the core.

Such cores are, in most cases, rather small, requiring especially delicate instruments. Where bushing-type current transformers are involved (and I presume Mr. Schwager was mostly concerned with these), the cores are large, making the determination of the electrical constants simple and accurate. The errors of these transformers are large, making a graphical method such as Mr. Schwager's particularly well-adapted. The adaptability of the method to modern compensated current transformers, where the errors are of the order of 0.1 to 0.3 per cent and one to three minutes phase angle, is not as good. There the reverse method may be helpful.

REFERENCE

1. SOME APPLICATIONS OF INSTRUMENT TRANSFORMERS, **Otto A. Knopp.** AIEE TRANSACTIONS, volume 55, 1936, May section, pages 480–9.

J. A. Elzi (The Commonwealth and Southern Corporation., Jackson, Mich.): The users of current transformers have a definite need for performance data covering a wide range of conditions which cannot be adequately covered by the usual ratio and phase-angle curves. It therefore appears that the user must be provided with basic data from which any desired characteristics can be determined, and, since most of the users are not transformer designers, it is important that these data be presented in a readily usable form. In this paper the author presents a means by which this can be accomplished and has analyzed step by step the method of arriving at this curve.

Since the admittance-vector locus is a function of the characteristics of the steel used in the transformer, it would appear that any one manufacturer would need to provide only a few such curves to adequately provide data for an entire line of current transformers. It would be interesting to have the author's opinion regarding this point.

The values for the points corresponding to various ratios of E_2/N in Figures 7 and 8 of the paper are not linear, and extrapolation between points is not possible. This at first appears to limit the usefulness of the curves. However, it might be pointed out that, for a given burden and turn, ratio values of secondary current can be chosen such that ratios of E_2/N corresponding to points given on the curve will result, and performance characteristics for intermediate values for secondary current can then readily be obtained. It might also be of interest to note that since the admittance-vector locus is located with respect to the unity power-factor line, it is very easy to investigate performance at burden power factors other than those shown in the figures by simply drawing in lines for any power factor desired.

G. Camilli (General Electric Company, Pittsfield, Mass.): The author's paper is timely in presenting a novel method of determining the performance of current transformers. The new American Standards Association Standards on instrument transformers recognize the fact that characteristic data of bushing-type current transformers for relay applications can be calculated from exciting current and core-loss data. By the method developed by Mr. Schwager, these excitation data can be incorporated in his admittance-vector locus, from which the final ratio and phase-angle errors can be derived.

This graphical method should be welcomed by both the designers and by the application engineers, because, in contrast with the standard procedure, it gives a more functional picture of the performance of the transformer. It is hoped that this method, together with other graphical analyses which have been proposed, may form the basis for a future ASA Standard for the calculation of current transformers for relay applications. The proposed method may be advantageous in all those cases where either the designer or the operating engineer is interested in complete characteristics at different burdens and at different power factors. However, it is doubtful whether the scheme reduces the amount of work involved over the standard procedure when only a small amount of data is required.

A. C. Schwager: Messrs. P. O. Langguth, E. C. Wentz, and G. Camilli point out the usefulness of the admittance-vector locus for the designing engineer; the question, however, is raised as to whether the method is practical for the use of the operating engineer when only a small amount of data is required. It appears that the author has not sufficiently emphasized the importance of Figures 9 and 10 in his paper. These figures represent the ultimate simplification of the vector-locus graph. Figure 9 shows that for a given burden power factor the ratio error of a multiratio bushing-type transformer can be represented by one single curve, provided $N^2/Z>1{,}000$. Figure 3 of this discussion shows a chart applying to an actual transformer from which the ratio for any total burden power factor between 0.5 and 0.7 can be easily computed as indicated for any practical value of N^2/Z.

The author intended to convey the existence of such simple charts in Figure 9 of the paper. Chart Figure 3 applies to a 600 to 5 multiratio transformer with the ratios as indicated. The top curve applies for a total burden power factor of 0.5 and the three lower curves apply to a burden power factor of 0.7 and varying N^2/Z values. A logarithmic abscissa of two different logarithmic rates for the E_2/N values greatly increases the accuracy over the entire range.

The practical result of Figure 10, on the other hand, is a simple chart Figure 4 of this discussion from which the ratio error can be read off directly in function of the

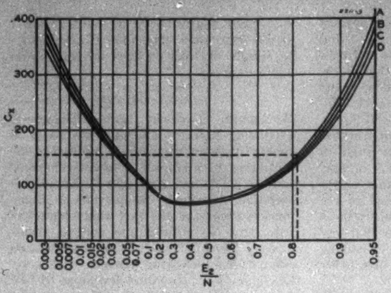


Figure 3. Ratio characteristics for 23-kv bushing current transformer

Burden power factor 50 and 70 per cent

Internal $R=0.0022\times N$ ohms

Internal X negligible

Volts per turn, $E_2/N=I_2Z/N$

True ratio $=N+C\times Z/N$

Curve	Range of N^2/Z	Power Factor (Per Cent)
A....	100–∞	50
B....	100– 400	70
C....	400–1,500	
D....	1,500–∞	

Ratio	Tap	Secondary Turns, *N*	N^2
50/5	*B-C*	10	100
100/5	*A-B*	20	400
150/5	*A-C*	30	900
200/5	*D-E*	40	1,600
250/5	*C-D*	50	2,500
300/5	*B-D*	60	3,600
400/5	*A-D*	80	6,400
450/5	*C-E*	90	8,100
500/5	*B-E*	100	10,000
600/5	*A-E*	120	14,400

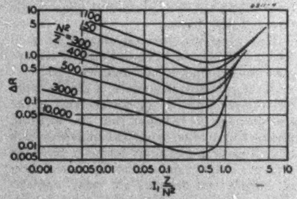


Figure 4. Ratio characteristics for 23-kv bushing current transformer

Burden power factor 50 per cent

Internal $R=0.0022\times N$ ohms

Internal X negligible

True ratio $=N(1+\Delta R)$

Ratio	Tap	Secondary Turns, *N*	N^2
50/5	*B-C*	10	100
100/5	*A-B*	20	400
150/5	*A-C*	30	900
200/5	*D-E*	40	1,600
250/5	*C-D*	50	2,500
300/5	*B-D*	60	3,600
400/5	*A-D*	80	6,400
450/5	*C-E*	90	8,100
500/5	*B-E*	100	10,000
600/5	*A-E*	120	14,400

primary current. N^2/Z is the parameter of the various curves; in a practical chart additional N^2/Z curves and scale subdivisions would be included. As shown in the paper, the individual curves in Figures 3 and 4 of the discussion can be readily obtained from the excitation characteristics of the steel, or from a sample of the transformer if available for test, the latter giving the more accurate data.

In view of the simplified charts Figures 3 and 4, the author considers it unnecessary to refer to Figures 7 and 8 of his paper or to the methods outlined by Mr. Wentz and by Mr. Langguth, except for design purposes or for application investigations of a more general nature. It might however be worthwhile to analyze some of the features of the above three methods.

Since we are primarily concerned with determining the ratio (complex) of the transformer, it appears that curves whose ordinates are equal to the actual error, except for constants of the transformer and burden are simpler than curves which are not proportional to the error. The admittance-vector locus represents the error, whereas if the magnetization curve is used, the error is obtained only after a division of the exciting current by a variable (I_2). In addition, the admittance-vector locus has increased reading accuracy over the magnetization curve.

In comparing Figure 5 of Mr. Wentz's paper with the author's charts, it must be realized that these charts are equivalent to the author's Figure 8 and do not cover the low-ratio bushing-type transformer represented in Figure 7, since Mr. Wentz uses a simplified equation for the ratio error.

The interesting charts A and B (Figures 1 and 2 of this discussion), which Mr. Langguth has submitted, correspond to Figure 7 of the author's paper. It appears, however, that Mr. Langguth's method requires considerable computation and drafting effort, in addition the range covered by the abscissa is only one per cent of that covered by author's Figure 7, and its practical value for multiratio transformers seems thereby seriously limited.

Since standardization of current-transformer data has been frequently mentioned in the various discussions, the author would like to summarize his suggestions; the final decisions as to the merits of the various proposed methods will, as pointed out by Mr. Wentz, have to be made by the application engineer.

It is proposed that two charts be furnished only.

1. Admittance-vector locus as shown in Figure 5 of the discussion, or a chart showing the admittance vector components in a rectilinear co-ordinate system, with E_2/N as abscissa in logarithmic scale.

   Figure 5 of this discussion permits the calculation of the exciting current for idle transformers and if desired can be used as the basis for any detailed investigation as described in the paper.

2. Ratio error curves as shown in Figure 3 or 4 of this discussion. Either of these curves permits the determination of the entire ratio data without complicated calculation or construction and require no knowledge of the operation of the transformer proper.

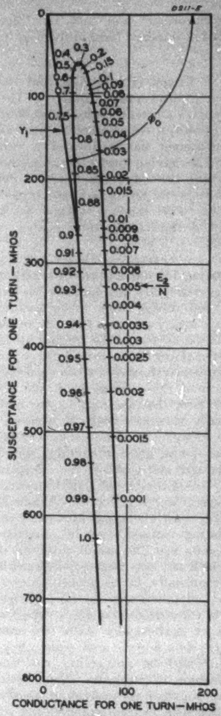


Figure 5. Admittance-vector locus for one turn for 23-kv bushing current transformer

Internal reactance negligible

Internal resistance $R=0.0022N$

$Y_N=Y_1/N^2$

Mr. Elzi points out an interesting further simplification which might have particular merit for design purposes. The admittance-vector locus can be rescaled so that $Y_{1(B)}=y_{1(B)}D/A$, where $Y_{1(B)}$ is unit admittance, A is the cross-sectional area of the core, and D is its mean diameter. Instead of a parameter of volts per turn, flux density B is used, or E_2/NA if preferred.

This basis will give values of sufficient accuracy for design purposes, even with cores of somewhat different proportions, and close values if the cores have equal outside-to-inside diameter ratios, because of similarity of flux distribution.

Mr. Elzi's concern about the difficulties encountered in interpolating E_2/N values on the admittance-vector locus might be reduced by referring to the proposed practical curves Figure 5, or by plotting the admittance-vector components on a rectilinear co-ordinate system as suggested above under standard data.

That the admittance-vector locus method is readily accepted by a utility laboratory is particularly appreciated. Mr. Knopp confirms the wide field for which the new method might be applicable. The determination of the vector locus in reverse, as suggested by Mr. Knopp, opens up a considerable field of applications. The method consists of measuring ratio error and phase angle for a known secondary burden and plotting them in a rectilinear co-ordinate system, the resulting curve being geometrically similar to the admittance-vector locus for one turn. This test curve, therefore, fully represents the transformer and permits the calculation of the various important constants, such as turn compensation and secondary leakage reactance.

Multichannel Carrier-Current Facilities for a Power Line

Discussion and authors' closure of paper 42-13 by P. N. Sandstrom and G. E. Foster, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 71–6.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The protective features of the carrier-current installation described by the authors provide an excellent example wherein the use of special, and sometimes so-called "complicated," protective measures are a principal factor in making possible important savings in major equipment (in this instance, the high-voltage oil circuit breakers). The value of such savings may be many times the cost of the additional protective equipment. The presence of continuous automatic supervision should add materially to the over-all reliability of the protective scheme.

Although the transferred tripping arrangement was provided primarily to permit clearing both power sources from a faulted transformer bank, the inherent abilty to clear both ends of the transmission line simultaneously is quite valuable. It is assumed that some form of distance relays is used for the phase protection in this connection. If so, is any special provision made to prevent operation of the distance relays during system oscillations, especially those which do not result in an actual out of step condition? Perhaps the multiple tone system described will lend itself rather well to an "out-of-step blocking" arrangement.

The protection of a three-terminal line or "Tee-tap" involves problems which sometimes cannot be solved by the simple addition of distance relays to a conventional carrier-current system. Have the authors made any study of the possible application of their transferred tripping arrangement to three-terminal lines?

P. N. Sandstrom and **G. E. Foster:** In answer to Mr. Brownlee's discussion, his assumption is correct that distance-type relays are used for protection against phase-to-phase faults. The particular type employed is the high-speed impedance type. No special provision has been made to prevent the operation of these relays during system oscillations. With the present method of operation there is very little chance for instability.

With reference to the question of protection of a three-terminal line with distance relays in conjunction with conventional carrier-current system, the authors have had contact with an installation of this type. This installation has given satisfactory service for over two years.

The authors have not made a complete study of the possible application of the transfer trip arrangement to a three-terminal line. One method of extending this transferred tripping method to a three-terminal line would be by means of a carrier loop consisting of three carrier frequencies. The first frequency connects station *A* to station *B*; the second frequency connects station *B* to station *C*, and the third frequency connects station *C* to station *A*. At each station only one transmitter and one receiver would be needed. The blocking loop would be extended over this carrier loop. Tripping would be accomplished by breaking the blocking loop at any point and starting the tripping frequency at that point. Since a loop is formed, the tripping signal would go to the other two points, giving almost simultaneous opening of all three terminals.

Transient Characteristics of Current Transformers During Faults

Discussion and authors' closure of paper 42-39 by C. Concordia, C. N. Weygandt, and H. S. Shott, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 280–5.

E. C. Wentz (Westinghouse Electric and Manufacturing Company, Sharon, Pa.) and **W. K. Sonnemann** (Westinghouse Electric and Manufacturing Company, Newark, N. J.): In the opening paragraph of this paper, the authors indicate that very little quantitative data on the transient performance of current transformers have been published. It is agreed that a complete set of transient-performance curves covering all existing designs of current transformers has never been issued. Such a set of curves would involve volumes. Apparently, this paper intends to present data which may be considered quantitative through the use of the so-called size constant of the transformer. In this respect, however, we do not feel that the paper has made any great strides forward over the Wentz-Sonnemann paper mentioned as reference 2. In the reference paper, the authors have presented formulas for the calculation of current-transformer performance in terms of design constants and secondary circuit resistance. In Mr. Harder's discussion of that paper, he reduced the design constants to terms of the saturation curve which puts the necessary design information in a form which can be obtained much more readily in many cases, and which can be used more easily.

We feel that the size constant as given in the paper is not too good a criterion of transformer excellence for the following reasons.

1. We believe that the factor, l, representing the length of the magnetic circuit, is unduly stressed, and tends to lead to erroneous conclusions. For example, if the factors l and A are both increased to, say, double their given value, the size constant is unchanged, but the transformer will, in practically every case, give better performance for any given application. This improvement results from the increase in area and not from an increase in length, as indicated in the author's conclusion 1. The reason for this is that the amount of flux in the core necessary to circulate a given current in the secondary circuit is independent of the length of the magnetic circuit. Once the flux reaches the saturation point, the error current begins to flow in the differential relay. If the area of the core is doubled, the time to reach saturation is increased, thus allowing an increased time interval for the d-c component of the fault current to diminish with a consequent reduction in the magnitude of the error current when it appears.

2. The factor, r_2, should and does include the resistance of the secondary burden. It inevitably follows that any one design of transformer does not have a single size constant in itself. Its size constant can only be determined after the secondary burden for the particular application has been ascertained.

The authors intimate that it is still necessary to consider each application of bus differential relays on its own merits. While this is true to a certain extent for some schemes, and where there are special considerations, it does not give a true picture of the situation as it has existed since the advent of the variable percentage differential relay. Sufficient study of the transient characteristics of current transformers and sufficient tests to verify the conclusions reached have been made so that in the usual bus-differential application, it is possible to apply the variable percentage differential relay without exhaustive engineering research. Such research is avoided through applying the relay in line with certain well-established criteria. These may be explained as follows:

1. The current-transformer ratio should be so chosen as to keep the maximum secondary fault current within reasonable limits as determined by a practical relay design

2. On multicircuit busses, it is sometimes necessary to use two relays per phase in order to provide an adequate number of restraining elements.

3. The current transformer should not saturate on the maximum symmetrical fault current to which it will be subjected. This does not mean that it will not saturate on asymmetrical fault current. Instead, the saturation caused by the d-c component will usually be severe, giving rise to a substantial error current. However, the use of the variable percentage characteristic as well as an adequate number of restraining elements prevents the relay from false tripping for external faults.

C. Concordia, C. N. Weygandt, and **H. S. Shott:** On reading over the discussion by Messrs. Wentz and Sonnemann, we note such a very great difference in viewpoint that it will be somewhat difficult to find a common ground for discussion. Messrs. Wentz and Sonnemann seem to feel that enough had already been done on this problem so that all the significant questions are readily answered, but we cannot agree with this opinion. To our knowledge the only quantitative results previously published have been from spot tests and often, as in the case of the paper by Messrs. Wentz and Sonnemann, without any means of identifying the transformer.

Referring to the first paragraph of the discussion, it is not merely by introducing the concept of size constant that our paper intends to present quantitative data, but also in a much more exact representation of the saturation curve. It seemed desirable, in view of the complexity and importance of the subject, to attempt as rigorous a solution of the problem as practicable, rather than to continue to oversimplify it.

Referring to the second paragraph of the discussion, we infer that Messrs. Wentz and Sonnemann cannot have given our paper the attention which we feel it perhaps deserves. As stated in conclusion 1: "Of two transformers having the same size constant the one having the longer magnetic circuit will give better performance for the same value of primary current." This statement is true, and to say, as the discussers have, that it is not the longer magnetic circuit which accounts for the improvement, seems to us to be quibbling. We do not subscribe to the discussers' arguments about the area; in fact, it could just as well have been the length l and the secondary resistance r_2 which were changed, instead of length and area, and conclusion 1 would still hold. The performance curves as shown by Figures 9–12 are all related to the "amperes per inch of core length." Thus, if length and area are both doubled, the size constant will remain the same, but the same value of primary current will result in half the ampere turns per inch, and, hence, the performance will be shown by a different point on the curves. Regarding the discussion of r_2, we refer the discussers to the definition of r_2 following equation 1 of the paper. Both the size constant and performance of any transformer depend on its secondary burden.

Referring to the discussion of the variable percentage differential relay it seems desirable to point out that such a relay would have severe limitations when used with conventional bar-type current transformers even when the current is only five times normal, as indicated by Figure 9.

Progress Report of D-C Testing of Generators in the Field

Discussion and authors' closure of paper 42-1 by E. R. Davis and M. F. Leftwich, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 14–18.

Charles F. Hill and **L. J. Berberich** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Davis and Leftwich have found with others that spot readings of insulation resistance were not as effective in determining the condition of the insulation as insulation resistance-time curves. Absorption or insulation resistance-time curves periodically determined have been used by many operators with some success. This success for the ordinary low-voltage determinations (500–1,000 volts) has, however, been limited to:

1. Detection of moisture in insulation.
2. Determination of leakage over the end windings due to moisture, dirt, oil, and combinations of these.
3. Studying the curing of the insulation in a new machine which is illustrated by the data in Figure 8 of the paper.

Low-voltage d-c testing on the other hand, has not been of much value in the location of damage to the insulation due to mechanical causes.

A number of operators have also attempted to locate mechanical defects by means of other tests such as power factor and ionization detection with negative results, in general. It had been our opinion that rather high-voltage d-c testing should be more effective in detecting mechanical deterioration in such insulation, but attempts by some operating engineers did not give positive results. It is, therefore, encouraging to see that the authors have achieved some measure of success with such high-voltage d-c tests.

The EEI subject committee on generator insulation testing (AIEE TRANSACTIONS, volume 60, 1941, p. 1003) reports some tests with high-voltage d-c on several machines which were about to be rewound. They were not always successful in locating deliberately inflicted mechanical damage. They did find, however, that insulation resistance may be considerably less when measured at high-voltage than when measured at a low-voltage in a machine known to have a defective winding. We have made many measurements of insulation resistance on sections of good coils insulated for 13.8 kv of both the older and the newer types over a range of voltage from 500 to 25,000 volts. These showed that the insulation resistance was substantially unaffected by voltage. Therefore, when a voltage effect is found, some insulation defect may be indicated. This should be investigated further.

The authors have logically started at lower voltages and gradually stepped up the voltages in their testing program, until now a voltage of 14,000 is used on machines of the higher-voltage ratings. We believe, however, that still higher voltages could be used. We have made a number of tests of the a-c and d-c breakdown strengths of both the older and newer type of insulation and have found that the d-c breakdown strength corresponds very closely with the a-c peak breakdown strength. The EEI subcommittee already referred to obtained the same result on old machines. It is, therefore, indicated that d-c voltages at least as high as the permissible a-c peak-test voltage on old apparatus could be used in testing without damage to good insulation. This would correspond to voltages up to 20,000 or 25,000 for a 13.8 kv machine which is somewhat higher than the maximum voltage used by the authors.

Messrs. Davis and Leftwich have indicated that many of the failures are due to mechanical causes. They have shown that high-voltage d-c has definite value in detecting this type of insulation defect. Table II of the paper shows that many of the breakdowns on test have occurred at d-c voltages far below the a-c peak-operating voltages. The authors state that some of these breakdowns have occurred on coils near the neutral. We would like to ask if all coils which broke down on test at voltages below the a-c peak operating voltage were far removed from the line end in the phase group.

The authors have apparently kept a record of service and test failures over a long period of time. Such records are of considerable value, not only to the operator, but also to the manufacturer for improvement in design may be suggested. These records are summed up in Table IV of the paper. One would expect that the real value of a testing program should be indicated by a reduction in failure rate in service. While Table IV does not give the service failure rate before the testing program was started, it does not show any consistent improvement in failure rate in service as experience was gained in testing. We would like to ask if the authors have any explanation for the lack of a definite improvement in service failure rate since the testing program was begun.

There is a possibility that machine voltages themselves are locating defects which develop on coils nearer full-phase potential, leaving only those on coils near ground potential for the d-c tests. If this is true, then the use of much higher d-c test voltages might be very useful to anticipate or eliminate service failures.

C. T. Weller (General Electric Company, Schenectady, N. Y.): During recent years, an important activity of the Institute has been the co-ordination of insulation. This involves testing methods as well as values, so it is gratifying to have a "Progress Report" on a d-c method to supplement the usual a-c methods. The use of high direct voltage for commercial routine testing dates from about 1916, when the development of our d-c cable-testing sets was started. These were developed

1. To provide equipment of greater portability than the ponderous a-c equipment then required for testing cable lengths.
2. To determine if impending cable failures could be anticipated from the time-current curves of the insulation.

The question of test equipment portability (1) does not ordinarily arise in the case of generators, but the question of impending failure detection (2) is always pertinent. After a long investigation, a ratio of 2.4:1 between the d-c crest and a-c rms test voltage values for cables was arrived at. A corresponding test-voltage ratio for apparatus apparently has not been established, so arbitrary values are sometimes used.

The effect of direct voltage on insulation is indicated by some of our experiences with the early cable-testing sets in the laboratory. We attempted to use sphere gaps to determine approximately the drop across the rectifier but found that the polarity of the direct voltage had an effect on the arc-over voltage. In view of the well-known limitations of sphere gaps from the standpoint of accuracy, we built an electrostatic voltmeter for use as a d-c–a-c transfer instrument; this was rated 100 kv (crest a-c or constant d-c value) and was of the oil-insulated, reflecting, repulsion type. The voltmeter worked beautifully with alternating voltage, but with gradually increasing direct voltage became more and more erratic, until it was impossible even to estimate what the deflection might be; the performance indicated that the leakage was both large and variable but no arc-overs occurred. In order to simulate cable lengths we built up a capacitor of glass-tinfoil units mounted in a wooden frame carefully treated with paraffin. As the direct voltage across the capacitor to ground increased, leakage through the seams in the wood finally caused smoke to appear, so additional insulators had to be provided. The direct-voltage values involved were much higher than those under discussion today, but the results were first responsible for our conclusion that direct voltage constitutes a much more severe test on insulation than alternating voltage when leakage is present. Variable leakage occasionally occurs in cable circuits and some very erratic arc-overs have resulted. However, it should be emphasized that cables, because of their uniform insulation and resulting voltage stresses, are ideal subjects for d-c testing, but such is definitely not the case for apparatus. It is evident that the authors were wise in proceeding with caution. It appears that many additional "Progress Reports" will be necessary before a proper d-c–a-c test ratio can be established for apparatus.

P. H. McAuley (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The author's results show that high-voltage d-c testing of insulation, which amounts to a combined dielectric and Megger test, is very effective. The following data may help to explain some of the results given in the author's Table III and Figure 8.

Table I of this discussion shows the results of tests at 500 volts d-c on four fractional horsepower 440-volt motors of a type which has given good performance in service. The tests were designed to determine the variations in resistance likely to be encountered on machines. The readings were at one minute after voltage was applied, but in most cases resistances were still increasing after 15 minutes. The corresponding currents may be obtained by dividing the voltage (500) by the resistance values shown. First, note the remarkable consistency in the performance of the four different motors. The first test was made at room temperature just as the motors were received from the factory. In a 48-degree-centigrade oven the resistances dropped slightly. However, with the oven at 75 degrees centigrade and later at 95 decrees centigrade, the values are practically the same as the original room temperature values. Upon cooling to room temperature the insulation resistances increased in about a 100 to 1 ratio. The last column shows the effect of standing around under summer air conditions plus an overnight cooling cycle in a refrigerator. All values, of course, are well up in the range regarded as indicative of insulation in good condition.

The apparent explanation of the behavior of the insulation is moisture absorption. When these motors were stored following the varnish bakes, a long slow process of moisture absorption followed under summer humidity conditions. Capacity and power-factor measurements also show this process rather well. At the higher temperatures moisture is being driven off and resistances do not change much. But when the motors cool again, insulation resistances in the dry condition at room temperature are very much higher. Exposure to room air plus a cooling cycle conducive to condensation soon reduces the readings to $^1/_{1,000}$ the former values. In spite of the varnish treatment, moisture influences leakage currents. Also, in these particular tests, the results were influenced in part at least by the leads which touched the frames at places in the terminal boxes. It has been found that rubber-insulated leads are no guarantee of high insulation resistance in the leads. In general, leads can have a great influence on indicated insulation resistances.

Table I. Insulation Resistance in Megohms After 1 Minute at 500 Volts D-C on 4 Small 440-Volt Motors at Controlled Oven Temperatures

Motor	29C	48C 5 Hr	75C 20 Hr	95C 19 Hr	22C 23 Hr	25C (18 Days) 5C (15 Hours) 25C (7 Hours)
1	460	125	430	415	22,000	27
2	475	119	400	445	65,000	16
3	455	99	340	360	42,000	25
4	495	106	310	340	38,000	31

These results apply, of course, to machines which stand idle for long periods and are subjected to high humidity conditions. On samples and on motors subjected regularly or continuously to high operating temperatures, insulation resistances usually increase with aging. It has been noticed that measurements at room temperature show about a 10 to 1 increase over considerable periods of time.

Individual values of insulation resistance above the range of values due to dirt or moisture can hardly be regarded as significant. However, similar machines seem to perform very consistently. If a given type is well-classified, insulation resistance readings may prove of some value in evaluating the condition of individual units. Admittedly we have a good deal to learn before this is possible.

B. Van Ness, Jr. (Pennsylvania Water and Power Company, Baltimore, Md.): In recent years there has been a growing interest in the development of a nondestructive method of checking the condition of generator insulation. The increasing demands upon standby and reserve equipment during the present emergency stress the need for a solution to the problem—which is complex and difficult—and one requiring the attention of many investigators. Therefore, the present paper is particularly timely.

Insulation troubles may be classified roughly under two headings:

1. Temporary—in which some outside agent temporarily reduces or impairs the insulation strength. In this class of trouble may be typed insulation affected by moisture and surface dirt.
2. Permanent—in which the dielectric strength of insulation is permanently reduced by mechanical breakage, electrical rupture, and so on.

The distinction between these two classes of insulation trouble is not always appreciated.

In general, the more commonly recognized nondestructive methods employed in machine insulation testing will rather definitely pick out the temporary types of machine insulation weakness introduced by moisture or dirt, but as yet they are not at all definite in the location of permanent types of insulation difficulties. Therefore, it is encouraging to see the fresh viewpoint presented in the paper by Messrs. Davis and Leftwich and the progress they are making in field testing. With the exception of destructive a-c high-potential testing, theirs is the first method reported to definitely pick out incipient insulation faults of the permanent type. In many cases of their checks it was not necessary to test to failure to detect faults, but in some cases the faulted winding actually failed during testing. This method of testing, therefore, does not as yet completely meet the desired ultimate of nondestruction, but it is a definite approach toward that goal. Winding damage is localized and the hazard of widespread trouble from service failure is eliminated. Furthermore, it is very possible that the test breakdowns occurring at the inception of the program might have been averted, had testing been under way when the insulation was first affected.

The higher test voltages offer interesting possibilities. It is hoped that the authors will find it possible to extend the method to the 13,800-volt class at comparable test voltages and with equally successful results.

E. R. Davis and **M. F. Leftwich:** The interest shown by those presenting discussions is greatly appreciated by the authors.

Dr. Hill and Dr. Berberich mention that some of the generator armature breakdowns on test occur on coils near the neutral. In checking our records, we find that many of the failures on test occurred approximately halfway between line and neutral coils in the phase group. The test voltage of failure usually exceeds the a-c peak operating voltage. However, at the beginning of the test schedule many of the incipient failures were located near the generator neutral at comparatively low test voltages so that no "standard" test voltage could be determined until these defective coils were located and removed.

Referring to the rate of failure in service as shown in Table IV, we wish to point out that the average age of generators prior to beginning of tests was 7.78 years as compared with 13.45 years to January 1, 1942. These ages take into account additional capacity added to system and machines which have been rewound. Further checking of records reveal that in the 10-year period immediately prior to test schedule, a total of 403,650 kva failed in service as compared with 360,115 kva as shown in Table IV. This difference of 43,535 kva is very encouraging, because the average age of windings during this 10-year period was increasing. It is believed with the newer test apparatus and additional testing experience this downward trend will continue and perhaps exceed the rate of decline in kilovolt-amperes failing in service as noted.

The difficulties which Mr. Weller encountered in accurately measuring high d-c potentials are easily understandable, although our problem was not so difficult, because we are using a comparatively low voltage. However, by keeping comparative records on individual generators, a slight inaccuracy of test potential as recorded by instruments would hardly be evident, providing the same test set and instruments are used.

The effect of moisture on insulation when a comparatively high d-c voltage is applied, as described in Mr. McAuley's discussion, is very interesting. We have used a d-c voltage of 5,000 on large 6,600-volt generator armatures while being dried prior to initial installation and have obtained similar results. However, a much longer interval of time was required to obtain a change in insulation resistance.

We hope that many high d-c voltage tests will be made in the near future, particularly on armatures of the 13,800-volt class. The compilation of such data may eventually lead to definite test standards. At least, as Mr. Van Ness, Jr., states, the higher test voltages offer interesting possibilities.

Measurement of Maximum Demand

Discussion and author's closure of paper 42-15 by P. M. Lincoln, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 57–62.

Perry A. Borden (The Bristol Company, Waterbury, Conn.): The soundness of the theory of measuring demand on a logarithmic basis has already been well taught by Dr. Lincoln in his earlier papers; and his original form of the instrument, even with the inherent errors which he has now pointed out, demonstrates the practical application of this theory and provides probably a truer basis for determining the limits of electrical apparatus than is found in the mechanical instruments of the block-interval class. I believe that those utilities who have adopted the thermal demand meter have found its use to be economically sound and are satisfied that such slight departures which it may possess from the theoretically correct values are insignificant as compared with the inherent departures of those values from being a true criterion of the actual limits of the apparatus. That demand measurement can never be reduced to an exact science is an admitted fact—and is probably the only fact which can account for the toleration of measuring methods which may show such vast discrepancies as have long been recognized and have been vividly brought to light in Dr. Lincoln's Ithaca tests.

In the development of the new form of thermal meter there has obviously been encountered the same problem that is met wherever the measurement of temperature is involved, namely, that of making sure that the temperature as indicated by the sensitive device is the same temperature as that in which we are interested. In this case, however, since temperature is but one link in a chain of energy transformations essential to the final measurement, the various loading and compensating devices appear to have succeeded not only in taking care of such discrepancies but in utilizing the time lag of heat transfer to simulate the temperature response characteristic of the electrical plant.

From an engineering standpoint, and even from an economic standpoint, the thermal demand meter would seem to approach as closely as possible to the ideal method of obtaining the desired measurement. The main objection to its use seems to be, now as heretofore, that of providing to the customer a satisfactory explanation of the engineering soundness and economic fairness of its readings. There has been raised against the use of thermal demand the argument that is readings are difficult to define and to explain in comprehensive terms to the nontechnical user. This objection probably has some foundation and would not in any way be removed by the adoption of the suggested modification in which, with the adoption of a longer characteristic time period, the meter is deliberately designed to depart from following a true logarithmic law. These objections, however, appear to be principally academic, and should not introduce more difficulty in the work of the rate maker than is found in the incorporation of a power-factor clause in charges for electrical service. Here, it is admitted that the possibility of several definitions might form a basis for controversy; but, as in most other instances of formulating definitions, a certain amount of compromise must be tolerated in the interests of ultimate simplicity. This attitude is well brought out in Professor Karapetoff's contribution, "A Parable About the Mean Radius of the Ellipse," published in the AIEE JOURNAL, volume 39, 1920, page 732. (I here take the opportunity to recommend that this parable be reprinted for the edification of the younger members of the Institute.)

The secondary importance of the actual value of the selected time interval in demand measurement has long been realized and was confirmed in the tests under actual load conditions as described in my 1920 paper on demand measurement (AIEE TRANSACTIONS, volume 39, page 1847). Dr. Lincoln, in his discussion of that paper, pointed out many of the features of demand measurement which have been brought to a head in his present paper. While in 1920 we were hardly so audacious as to suggest anything like the universal adoption of a 60-minute time interval, it now appears that such a period, especially if incorporated in a logarithmic meter of the suggested form, might fill a very large place in the measurement of demand.

W. J. McLachlan (General Electric Company, Schenectady, N. Y.): The condemnation of the block-interval meter in Mr. Lincoln's paper appears practically to incriminate the prevailing practice in this country. This allegation at face value is alarming. That it is not, is evidenced by the fact that the same theoretical shortcomings of the block-interval type were expounded, before the Institute, quite thoroughly by Mr. Lincoln in 1915, again in 1918, again in 1934, again in 1939, and now are repeated in 1942.

Surely, there must be some reason behind the continuing use of a device whose shortcomings have so long been so well understood. In fact, there are many reasons. Among them are:

1. Any measurement of demand constitutes not an exact, but rather, only a rough approximation of the proportion of the system investment cost incurred in supplying that demand. This has been recognized practically since the conception of demand rates.

2. Since no demand allocation method known is exact, there has been a preference for a measurement basis which is simple to understand and explain. This is a definite advantage of the arithmetic average of the block-interval type as contrasted to the logarithmic characteristic.

3. "Peak splitting" is of significance only on load peaks of short duration, which occur singly during the billing period or which repeat at an interval exactly equal to the "block interval" or a multiple thereof. Probability states that random repetition of peaks within the billing period, which is the usual case, will minimize the "peak-splitting" effect.

4. Short-duration loads, where peak splitting can possibly have any effect, and where advantage is claimed for the logarithmic characteristic are a problem unto themselves, because the ability of the system to supply such loads is limited generally by voltage drop and not the thermal ability of the system. The voltage-drop effect is instantaneous, and its import cannot be measured by any of the time-interval demand measurements now used, be they logarithmic or arithmetic.

5. While heating of system equipment follows a logarithmic characteristic, the time constants involved in the various equipments are so radically differing in themselves, and different from those of the logarithmic meters now employed, as to make any claims for advantage in this respect at least open to question. For instance, a transformer may require six to seven hours to reach 90 per cent of ultimate temperature, as contrasted to the one-hour meter characteristic. Actually, an arithmetic curve much more nearly approximates the heating of such an equipment during a one-hour interval than does a one-hour logarithmic meter curve.

6. The fact that one type of meter follows the heating curve of equipment, at one fixed value of load, more closely than another is of little significance when it is considered that the response of both meters, to differing values of load, is in accordance with the first power of the current (since the measurement is in kilowatts, kilovolt-amperes, or reactive kilovolt-amperes) whereas the heating of equipment is much more nearly proportional to the square of the current. Thus the basic measurement is not in accordance with the thermal loading of equipment.

7. Testing convenience has been a predominant advantage of the block-interval type.

The discusser holds no brief for the block-interval meter. It is the manufacturer's business to build the types of equipment which the trade chooses to desire. The purpose of this discussion is to record the fact that there are factors which should be considered in arriving at this choice which do not appear in Mr. Lincoln's paper.

P. M. Lincoln: I take positive exception from the comments submitted by Mr. W. J. McLachlan. The indication of maximum demand as registered by the thermal demand meter is completely exact and not a rough approximation as Mr. McLachlan would have us believe. Its only departure from complete exactness is due to the impossibility of causing heat to diffuse instantaneously throughout the masses of matter that are being heated or cooled during normal operation. By careful design, this departure from complete exactitude can be made negligible.

May I ask Mr. McLachlan why he advocates an admittedly inaccurate method of measuring maximum demand when an exact method is available?

I would also disagree with Mr. Perry A. Borden's statement "that demand measurement can never be reduced to an exact science is an admitted fact." This statement is quite true when applied to an arithmetic average, but almost entirely false when applied to a logarithmic average. I, for one, make no such admission. The thermal demand meter as described in my paper is absolutely exact except in so far as its indication is influenced by the impossibility of causing heat to diffuse instantaneously. Apparently, both Mr. McLachlan and Mr. Borden fail to recognize the basic difference between a continuous and discontinuous function of time. The indication of the thermal wattmeter is absolutely exact except in so far as above indicated.

My paper is, perhaps, more unique for what it omits than for what it contains. For instance, in column 4, there appears a mathematical expression for the difference in the amount of heat that enters the two elements of the thermal wattmeter during normal operation. This expression is incomplete. The factor cos ϕ should be added where ϕ is the phase angle between E and I. Instead of $2EIr$ as given it should read $2EIr \cos \phi$. In other words, the thermal wattmeter is a true wattmeter independent of power factor. I did not include the factor cos ϕ in my paper because of the many questions which such an inclusion might have raised. For instance, at power factors other than unity, what is the best relationship between the factors E, I, r and cos ϕ? At unity power factor, the best relationship is to make the influence of the factors E and I approximately equal. However, we must bear in mind that at 50 per cent power factor, the influence of the factor I is twice that at unity power factor; at 25 per cent power factor, four times; at 10 per cent power factor, ten times. This naturally raises the question what is the best relationship between the factors E, I, r and cos ϕ for the usual conditions of service that are met in normal operation. If all these questions had been discussed to the extent their merits dictate, my paper might have lengthened to many times its present length, and, in my opinion, it is already long enough. In view of this undue possible lengthening, it seemed better to omit the factor cos ϕ from the expression in column 4.

Freedom from power-factor error is obtained only by exceeding care in the design of the small transformer used. The capacity of this small transformer is very small. In any transformer there is a phase difference between primary and secondary. The smaller the transformer, the greater, in general, is this phase difference. By careful attention to transformer design, I have been able to keep the phase difference in this transformer down to a matter of two or three minutes of phase angle. This phase angle is small enough so that the resulting power-factor error is negligible even on very low power factors. It is worthy of note that exceeding care must be taken in this small transformer design.

In column 14 there is given a mathematical expression for the indication of the thermal wattmeter. Due to my own inadvertence, the text of paper does not sufficiently explain the full significance of this expression. This expression gives the indication of the thermal wattmeter when a given amount of energy (watt-hours or kilowatt-hours) is passed through the meter in a given time t. Also, this expression holds true only if the wattmeter is indicating zero at the time the load is applied. If the meter indication is other than zero at the time of load application, the final meter indication is vastly different than if its indication is zero. If the rate of load application is lower than the meter indication at the instant of load application, the demand wattmeter indication actually decreases. Also, the expression holds true only if heat diffusion is assumed to be instantaneous.

I wish to call particular attention to the last sentence in my paper. I am strongly of the opinion that the time interval for demand measurement should be standardized. I see no reason why John Jones should be assessed for maximum demand on the basis of a time interval that differs from that of Jim Robinson. The real object of any demand assessment is to assess the service user an amount that will reimburse the utility for its fixed charges. It is perfectly true that if John Jones has a perfectly steady load, while Jim Robinson takes his load at a rate of ten times or 100 times that of John Jones, but during only a small fraction of his time interval, then Jim Robinson should pay more for his service than John Jones. The thermal-demand wattmeter automatically recognizes the difference between John Jones' method of taking service and Jim Robinson's. The standard block-interval meter does not.

It is further my opinion that the standard block-interval demand meter must be scrapped. When it is possible for a service user to reduce his maximum demand assessment to one-half his actual maximum demand, the method of maximum demand measurement by which this becomes possible must be scrapped. This is particularly true since it is quite possible for the service user to accomplish this end without the utility even becoming aware that it is being done. In my opinion, this scrapping should have occurred back in 1917 when the thermal-demand wattmeter first appeared. During this 25 years, the public utilities of the United States have expended something of the order of $100,000,000 for demand-metering equipment. However, perhaps there has been no large loss by the public utilities of the United States in this delay of 25 years in scrapping date since much has been learned concerning the character of the thermal-demand wattmeter during this 25 year period. For instance, in 1917 when the thermal-demand wattmeter was first produced, there was no known means of adjusting its time interval. The thermal-demand wattmeter was introduced into Canada about 1919 and has become the standard demand wattmeter of Canada. In Canada, however, the time interval is approximately 15 minutes—the time interval of the thermal wattmeter as first produced. The longer time intervals have not yet been introduced into Canada. In my opinion, 15 minutes is much too short a time interval for demand measurement. As indicated in my paper, I favor adoption, as standard, the maximum time interval now being used, 60 minutes. If the thermal type of demand wattmeter had been adopted as standard in the United States in 1917, the time interval would unquestionably have been 15 minutes and we in the United States would have found ourselves in the same position as Canada now is—too short a time interval.

In adopting a standard time interval we must also give due consideration to ammeters as well as wattmeters as pointed out in my paper. Shall the one-hour thermal-demand wattmeter be the one that arrives at 90 per cent of final in one hour, or 81 per cent? Although I have never actually produced a thermal-demand wattmeter that will arrive at 81 per cent of final indication in one hour, my experimental work has clearly indicated that there will be no difficulty in producing such a meter. I personally believe that it would be preferable to keep the wattmeter's time interval at the time to arrive at 90 per cent of final and change the ammeter's to 95 per cent, but my opinion on this question is not so fixed as upon some of the other questions discussed herein.

It might further be pointed out that the thermal-demand meter may readily be so connected as to indicate vars or kilovars as well as watts or kilowatts. It may thus be used to take account of the power factor of the load under measurement. I think rate makers will generally agree that at the present time, the power factor of the load is not recognized to the extent that it deserves. By the measurement of vars or kilovars as well as watts or kilowatts, power factor can be recognized to whatever extent the rate maker may think advisable.

In my paper, as well as in this closure, I have expressed some rather pronounced, perhaps radical, opinions. If anyone commenting thereon wishes to disagree with any of those opinions, here is the place, and now is the time to express those disagreements. I have quite often found myself in disagreement on the matters discussed herein, and a few more added to those disagreeing with me will be received with complete composure. Let me urge those disagreeing with any of my expressed opinions to have no hesitation in saying so.

The Acceleration-Oscillogram Method of Motor-Torque Measurement

Discussion and authors' closure of paper 42-5 by C. R. Atkinson and E. G. Downie, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 7–9.

R. W. Ager (Cornell University, Ithaca, N. Y.): I wish to welcome the paper by Messrs. Atkinson and Downie to the literature on acceleration testing. Every method has its own inherent advantages. The main advantage of the method using the velocity-time curve as suggested seems to be in the use of standard equipment and in the speed with which calculations can be made. However I would like to ask the authors what accuracy can be obtained by this method. The authors have not shown how widely scattered the calculated points might be, nor do they indicate how successive calculations made from the same film might check if, perhaps, made by different calculators.

I wish to call attention to the differences in the shapes of the two curves of Figures 6 and 7 of the paper. These were taken from tests[1] made at different voltages and, therefore, some difference in shape might be expected due to saturation. While in my tests I found some differences in curves taken with a similar spread of voltage, the curve shapes were not as varied.

The authors' curves show a dip in the torque slightly above 50 per cent synchronous speed. Similar results have been found in my tests on some motors. A dip can be caused by dissymmetry in the electric circuit of the rotor as indicated by Figure 2 of my paper. This curve was for a wound-rotor induction motor operated with an unsymmetrical controller. Pronounced dips were also found in the curves taken on a 15-horsepower synchronous machine. The three large synchronous motors tested, however, showed no indication of having such dips in the torque curves, although the accuracy of the tests was such that even slight dips would have been recorded had they been present.

The torque pulsations indicated in these tests can occur due to two causes. These are:

1. The salient pole structure of the magnetic circuit.
2. Any dissymmetry of the electric circuits on the two axes.

Both of these cause pulsations of the same frequency, namely twice slip frequency. A comparison of the pulsations indicated in Figure 3 of the paper with those indicated in Figures 1, 2, and 4 will show that the latter are much more pronounced. In Figure 3 the speed curve does not show a negative slope until synchronous speed is reached. From an electrical point of view there seems little reason for this difference.

A careful examination of Figures 1, 2, and 4 will show that in these tests the frequency of the pulsations did not vary greatly. Also, the same period was indicated for the torque transient at starting (except in Figure 4 where the transient was too small to show clearly). Figure 3 indicates a different behavior. The starting transient shows only slight pulsations, and the main pulsations between half and full speed have a continually decreasing frequency which is exactly twice that of the field discharge current. The pulsations of Figure 3 check with the expected performance, the others do not. In taking the curve of Figure 3 the motor was operated without the added inertia of the second machine which was used in the other tests. With the second machine coupled to the motor, a torsion pendulum was formed which was set in vibration by the sudden application of the starting torque and also by the torque pulsations when the latter were of the proper frequency. This resonance effect probably accounts for the building up of the large swings noted in the tests. The high shaft torques corresponding to the indicated values might be present in generator sets driven by synchronous motors. That they are so well shown in these tests would indicate an application for this type of testing.

Reference

1. Some Acceleration Tests on Large Synchronous Pump Motors, R. W. Ager. AIEE Transactions, volume 60, June section, pages 416–22.

John F. H. Douglas (Marquette University, Milwaukee, Wis.): This paper is of interest to my colleague, Mr. E. A. Halbach of Marquette University, and myself, because we have been trying to perfect a generator and oscillograph which will measure and record torque-time and torque-speed curves. We submit two oscillograms, among our best, but still with too much ripple due to defects of generator construction. The first is the torque-speed oscillogram of a five-horsepower century induction motor started from the line. The second is the torque-time oscillogram of a synchronous motor. This shows in sequence the 50 per cent speed-torque dip, the increased torque on changing to full voltage, and the hunting pulsations caused by closing the field switch. We consider our oscillograms superior to oscillogram 5*B* of the paper, but inferior to those published by Dannath and Redfearn.[1] Dannath and Redfearn took extraordinary pains to build a ripple-free tachometer generator. Most of the other writers[2] on this subject also attribute the ripples to defective generator construction. When a Prony-brake retardation test of any motor is taken, and the ripples show up prominently, it is a good indication that the device is not sufficiently developed to measure real torque ripples of smaller magnitude.

The writers have undertaken a very difficult subject. While they furnish but little data on the device, it must be remembered that it would take a 500-microfarad capacitor, with a 100-volt tachometer, and a standard oscillograph, to record the acceleration of a motor starting in one second.

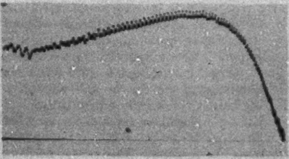


Figure 1

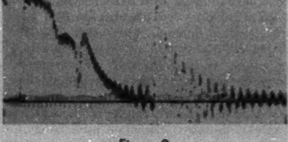


Figure 2

We believe the authors should furnish constructional features, the resistance and inductance, of the generator, the capacitance of the capacitor used, the sensitivity, resistance, and time constant of the oscillograph, if their generator is superior to Dannath's. In any case they should publish a sample Prony-brake retardation oscillogram to demonstrate that their system is free from undue ripples inherent in the tachometer generator. It is hoped that the discussion on this paper will bring out desirable specifications for an instrument or system of the type described.

References

1. **C. Dannath and S. W. Redfearn.** *Metropolitan Vickers Gazette*, volume 13, October 31, 1932, pages 82–5.

2. **R. R. Owens.** *Canadian Electrical News*, volume 15, December 1905, page 298; *Electrical Review*, volume 47, December 9, 1905, page 908; Electrical Engineering, volume 39, December 1905, page 904.

T. M. Gardner. AIEE Proceedings, volume 35, July 1906, page 453.

A. P. Young, *Electrical World*, volume 49, March 16, 1907, page 551.

C. R. Moore. *Electric Railway Journal*, volume 35, February 5, 1910, page 227.

Ytterberg. *Elektrotechnische Zeitschrift*, volume 43 November 7, 1912, page 1158.

Knauer and Schultze. *Elektrotechnische Zeitschrift*, volume 43, October 26 and November 9, 1922, pages 1307–11, and 1357–9.

H. W. Bousman. *Michigan Technic*, January 1927, pages 20–32.

L. A. Finzi (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The determination of speed-torque characteristics of motors from acceleration oscillograms seems to deserve broader application on test floors. When average torque values are being considered, the calculation of acceleration by graphical differentiation of speed-time records may be sufficient. But for instantaneous values a direct electrical differentiation method is desirable.

Many methods including the one suggested by the authors have been used for this purpose. The authors record the charging current from a d-c tachometer to a capacitor. Errors are caused by the resistance and losses of the circuit. A more serious objection is the voltage pulsations from the tachometer collectors, which are difficult to smooth out and sometimes cause misleading ripples in the oscillographic record of the capacitor current. Other methods use mutual inductances to give secondary voltages proportional to the variations of the voltage applied to the primary.

Very good records may be obtained by applying the tachometer voltage to a D'Arsonval galvanometer, which has a second coil wound on the moving frame. The voltage induced in this second coil is recorded with the oscillograph. If α is the deflection produced in the galvanometer by a given instantaneous value of tachometer voltage, the voltage induced in the second coil by its motion in the field will be proportional to $d\alpha/dt$ and hence to the acceleration of the motor shaft. The construction of such a galvanometer (originally developed by Heiles at Karlsruhe University around 1924) requires certain care, as the inertia of the moving parts must be kept low, and quite a number of turns are required on the secondary coil. However, the adaptability of the instrument, when used according to the proper galvanometric technique, and the smoothness of the curves obtained make it very useful, especially when instantaneous peculiarities of torque curves are to be investigated.

Finally as a variation of the capacitor method, a small freely rotating d-c motor, with constant field and with the armature fed by the tachometer voltage, may be used. A change of speed of the tachometer causes a transient current which is again proportional to the acceleration or deceleration of the main motor shaft. In view of the comparatively large currents available in the measuring circuit, this method may be useful for such applications as acceleration governors. References 1 and 2 of this discussion may be of interest.

REFERENCES

1. **A. Ytterberg.** *Elektrotechnische Zeitschrift*, 1912, page 1158.

2. A METHOD FOR MEASUREMENT AND CONTROL OF ANGULAR ACCELERATION, **L. A. Finzi.** *Annals* of Naples University, 1935.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): The method proposed in this paper of using the acceleration speed characteristics to translate back into torque data is a very good and powerful one. Many things are being learned by it, but up to now the limitation has been in obtaining faithful speed indications. The authors are to be complimented on being able to produce usable records of speed through the means of their special tachometer generator. The use of such a generator will doubtless eliminate difficulties of other devices which introduce large losses of their own and thus mask the effects being sought. The direct acceleration record, through the charging current to a capacitor in parallel with this generator is especially interesting. The application of this generator to deceleration tests will extend the potentialities of that method to smaller machines. Another application which suggests itself is induction machine torque-speed tests, where torques that depart from conventional theory are only recently being recognized.

Of particular interest to this discusser are the torque curves obtained on small educational motor-generator sets, and their peculiarities discussed in appendix C of the paper. The peculiarities at about half speed which the authors found difficult to believe heretofore are altogether too common on small machines, and are particularly evidenced by the sounds which accompany this acceleration period. In the electrical engineering laboratory at Worcester Polytechnic Institute, most of the small sets exhibit some dips at half speed but usually pass by this point without difficulty. One machine, however, a 12.5-kva AHI synchronous motor, has shown particularly bad characteristics. At reduced voltage the machine would lock in at 58 per cent of synchronous speed and not accelerate further until thrown over to full voltage, even then being reluctant to pull up to full speed from that point. Incremental torque available for further acceleration was apparently zero at that speed.

Investigation of the factors giving rise to this extreme dip indicated that they relate to:

1. Applied voltage.
2. Resistance in the field circuit.
3. Temperature of windings.

The last factor only aggravated conditions on repeated tests, and so will not be discussed further. The circumstances of this machine were that it had recently been converted from use as an independent synchronous motor into a unit of a motor-generator set. The additional demands on its starting characteristics now made the problem acute where it had been only bothersome before.

Voltage taps were then changed from 65 per cent to 80 per cent of rated applied voltage with beneficial results but still with a long hesitation as it passed through the critical speed. Line-start procedure was then employed. These results indicated that voltage was not alone at fault, as the improvement was not in proportion to that expected from the voltage-square relation.

The field discharge resistor was 25 ohms originally. In desperation, it would sometimes be opened to permit the machine to accelerate. Obviously, this procedure was undesirable, but it proved the source of the trouble to be in the phase relationship between air-gap flux and rotor-circuit currents controlled by the short-circuited field. The resistance was changed successively to 50 and then to 75 ohms, the current at starting remaining substantially constant at six amperes. Improvements thus obtained indicated that the currents at standstill were being swung from nearly 90 degrees to nearer 45 degrees lagging with respect to the flux. The machine would thus accelerate easily through the dip point, which was raised to 60 per cent speed, and groans caused by torques were considerably reduced. The conclusion of this exploration would be that field circuits on synchronous motors should be not short-circuited but closed through the highest resistance that can be tolerated without endangering the insulation.

There is considerable similarity between synchronous-motor starting and induction-motor performance, and they are subject to similar methods of analysis. Torques are produced by components due both to fundamental frequency fluxes and to the harmonic fluxes which give rise to stray load loss. The resultant torques obtained by this complex pattern would seem to this discusser to permit resolving them into positive sequence (forward) torques due to the amortisseur winding and field circuit currents, and negative sequence (reverse) torques due to harmonics and other field circuit effects. Below half speed the positive torques predominate, being aided by field effects. At half speed, certain of the forward factors become reversed and aid the negative torques until, at some speed slightly above 50 per cent, the two opposing torques produce only enough resultant torque for the requirements of that critical speed, with none remaining for acceleration.

It is to be hoped that the "tools" developed and the interest aroused in this subject will lead to a clarification of the factors involved in a-c motor starting torques, as well as the many other studies which this acceleration-oscillogram technique will make possible.

R. M. Saunders (University of Minnesota, Minneapolis, Minn.): It would be interesting for the authors to point out the differences between their method of torque measurement and the acceleration method outlined in the "Proposed Test Code for Synchronous Machines" (1937), page 20, paragraphs 130 and 131.

In Figures 6 and 7 of the paper the authors show a disagreement of as high as 100 per cent in the torque measurements on the machine under test and suggest that these errors are due to heating of the armature and amortisseur windings. If this were true, the resistance of the amortisseur winding would be approximately doubled, and the temperature rise associated with this doubling of the resistance would be in excess of 250 degrees centigrade. This seems to be out of line with the usual industrial practice in regard to limitation on the temperature rise of cage windings. The writer would like to ask the authors if some other errors, such as transient effects, could not have crept into the other torque measurements.

In using the authors' form of the acceleration method, the accuracy depends upon two factors: the WR^2 and the readings of $\tan \theta$. In regard to the former, variations in the casting of spiders often make the calculation of the WR^2 subject to variations of as much as ten per cent. Extremely accurate calculations may be made and the result should be within five per cent. In regard to the latter quantity, the values taken from an oscillogram are apt to be in error due to the small physical measurements, the width of the trace, and the actual calibration of the oscillograph. In the writer's opinion these could easily amount to a ten per cent variation in the final result, even if the observers were careful. With the Prony-brake method an experienced test floor man can duplicate his results within ten per cent. Hence, the Prony-brake method should be equally accurate and somewhat more direct, which seems to make it preferred on machines up to 500-horsepower.

C. R. Atkinson and **E. G. Downie:** Interest has been shown in the exact electrical characteristics of the d-c tachometer generator used in our acceleration torque tests. The machine has a resistance of 51.7 ohms, inductance of 0.512 henry, and generates 0.155 volt per rpm. When running at 1,700 rpm, generating 263 volts d-c, it was found to have principal ripple voltages of 0.25 volt at 56 cycles or double rotational frequency, and 2.3 volts at 680 cycles commutator frequency. A ten-microfarad capacitor reduces the commutator ripple voltage to 0.04 volt (invisible on the speed trace), yet does not affect the amplitude of generated frequencies below 40 cycles. To get a reasonably smooth trace of capacitor charging or acceleration current requires 100 microfarads across the tachometer generator output, in addition to the smaller capacitor in series with the galvanometer. Response is satisfactory then, only to frequencies of 20 cycles and less. Slightly higher frequencies are amplified by series resonance, and still higher are greatly reduced.

We have been interested in the average

torque curves of synchronous motors, and have found the speed curve with slope measurement simpler to take and satisfactory in results. All calculated points fall almost exactly on the curves as drawn in Figures 6 and 7 of the paper. Torque pulsations are averaged out of the speed curve before scaling. The over-all accuracy should be ten per cent or better. We question the possibility of equal accuracy or even the practicability of full-torque-curve Prony-brake tests on motors up to 500 horsepower, as suggested by one discusser.

The oscillogram of Figure 5 of the paper was taken with 12 microfarads in series with the acceleration galvanometer, and a 100-microfarad filter capacitor. Resonance in the tachometer-capacitor circuit is calculated at 21 cycles, with maximum capacitor ripple voltage rise of 31 per cent over the generator voltage. The oscillation frequency of the acceleration trace, at its maximum amplitude, agrees with the synchronous-motor pole slip frequency, and also with the double rotational ripple frequency of the tachometer. This agreement, with proximity to circuit resonance does leave the amplitude of the acceleration oscillation questionable. We have noted with interest the methods of acceleration indication described by discussers and intend to investigate their possibilities.

We are pleased to note the agreement of discussers with our findings of the synchronous-motor torque dip at 60 per cent speed. Mr. Ager's explanation of the increased torque pulsations in Figure 2 of the paper, over those in Figure 3, appears very plausible. The homopolar generator used by Dannath and Redfearn is doubtless the most ripple-free yet devised, but the required d-c amplifier considerably complicates the system. Absolute linear response of the amplifier must be assured, or any deviation known and corrected for.

Design of Long-Scale Indicating Instruments

Discussion and author's closure of paper 42-70 by A. J. Corson, R. M. Rowell, and S. C. Hoare, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 318–27.

M. S. Wilson (General Electric Company, Schenectady, N. Y.): The development of the complete line of long-scale instruments represents an outstanding accomplishment in the measurement art.

The goal the instrument designer is continually striving for is to obtain the maximum accuracy in the smallest size instrument, having a readability which is suitable for the application.

The table below shows a comparison of the General Electric long-scale instrument with the conventional instruments, on the basis of area versus scale length. This shows a nearly two to one gain in scale length for the same size instrument, and a scale length nearly 50 per cent longer than the six-inch-size instrument requiring more than twice the switchboard area.

It is interesting to note that full advantage was taken of the newly developed materials in each of the different types of elements. It would have been very difficult to have developed instruments of this type having satisfactory characteristics without the use of these materials. One important point in the use of magnetic materials in the field circuits of instruments is that the mechanical structure be designed so that it is mechanically stable, and where air gaps are necessary, they be sufficiently large so that minute shifts in the structure will not adversely affect the operation or accuracy of the instrument. The writer is familiar with the designs described by the authors and considers this detail to be well taken care of.

Table I

Type	Switchboard Area Required by Instrument (Square Inches)	Scale Length (Inches)
General Electric long-scale types *AB*-10 and *DB*-10	18.1	6.8
Typical 6-inch rectangular-size instrument	44.5	5.1
Typical 4-inch rectangular-size instrument	18	3.5

It is particularly noteworthy that no sacrifices in the instrument characteristics were made in order to obtain the long-scale feature, in fact, in two important respects, (*a*) in torque to weight ratio, and (*b*) shielding, the figures are better than usually found in switchboard instruments. In only one instrument, the temperature indicator, is the torque to weight ratio less than one. In this instrument it is 0.92 as compared to 0.5 which is frequently found in this class of instrument.

With the higher currents in conductors near the switchboard, the shielding of an instrument becomes of greater importance to the user. In the instruments described by the authors the errors shown in the Table 2 under "Stray Field Influence" do not exceed one-half the permissible error given in American Standards Association Standards, and for the majority of the instruments, it is appreciably below one-half the value given by ASA.

The other characteristics such as response, temperature coefficient, overload performance, and so on, are likewise such as to entirely meet the requirements of the various applications, where a universal instrument of this type may be used.

One further point which is of considerable importance to the user, but which is not covered in any existing specifications, is the stability of the mechanical structure of the instrument element. This is very important since it determines whether or not the instrument will operate satisfactorily maintaining its calibration over long periods of time. To do this the clearances between moving and fixed parts must be correctly and permanently maintained.

This means that the element must not only be so designed that it is structurally stable but the different materials must be carefully selected from the standpoint of temperature coefficient of expansion and aging characteristics so that they will not adversely affect the operation of the instrument even when subjected to severe operating conditions.

Paul MacGahan (Westinghouse Electric and Manufacturing Company, East Orange, N. J.): The paper by Messrs. Corson, Rowell, and Hoare is of special interest as another example of the fact that progress often proceeds in spirals—advancing bit by bit, after turning and returning to almost the same point.

Thus, the writer in his paper presented in June 1912, at the annual convention described a complete and harmonious line of long-scale a-c instruments.[1] A reference to this paper might with advantage be added to the bibliography given in the present paper for the sake of the record and to indicate the progress of the art.

In the discussion of the 1912 paper, Mr. F. P. Cox of the General Electric Company stated as follows: "The long scale which is inherent in the type is a desirable feature, but we must not neglect to take into account that what we want to know is the current in the circuit, that if you can read an instrument beyond its limits of accuracy, or if you have an instrument which has an error greater than its reading capacity, you equally get into trouble. What you want is an instrument which will give you, so far as you can read it, the combination of being readable and possessing an accuracy which will give the closest approximation. . . ."

Mr. Cox was right in 1912 in pointing out that a scale length which gives a readability greater than justified by the accuracy of the movement is unwarranted. In my closure I stated that the long scales were an advantage because instruments had to be read from a distance. But, as time went on, the old-fashioned switchboard gave way to the compact control disk type with instrument panels which brought the instruments close to the operator. For this reason as well as for economic and application reasons, the long-scale induction instruments of 1912 were by 1920 superseded by more modern types having scale lengths well calculated to be normal with respect to the accuracy of the measuring element.

A little analysis of the problem of scale-length factor in over-all accuracy bears upon the fact that with the highest grade of construction a certain amount of side shake in the bearings is inevitable because of the end-play requirements in the ordinary horizontal-shaft instruments. This could be and sometimes is reduced in vertical-shaft portable instruments by reverse-pivoting, with the top pivot supporting the weight. But such structures are obviously impractical in switchboard types, if we except the older forms of horizontal edgewise instruments.

The end play and consequently the side play has approximately the same value in all ordinary sizes of practical instruments. This side play is generally of the order of 0.010 inch and allows this amount of uncertainty in the pointer position. The result may be approximately expressed as amounting to an error equal to $1/L$, where L is the scale length in inches.

This then is that portion of the total error in a well-designed instrument which can be reduced by increasing the scale length.

Compared to the usual and most common

form of rectangular switchboard instruments which are about $5^{1}/_{8}$ inches square with scales five inches long, the authors have increased the length of scale to 6.8 inches, by using the circular dial construction instead of the flatter 100-degree dial design. To do this has required the development of entirely new measuring elements, a job, the magnitude of which, is not generally appreciated by the user.

A one per cent accuracy rating amounts to over $^{1}/_{16}$ inch on a 6.8 inch scale. Switchboard instruments having the usual one per cent accuracy rating are also produced with scale lengths of $3^{1}/_{2}$ inches in rectangular cases of the size described by the authors. As one per cent of such scale lengths is about $^{1}/_{32}$ inch they are quite adequate for the rather less frequent applications for which a four-inch switchboard instrument is preferred to the $5^{1}/_{2}$-inch type, and where readings within one per cent may be taken with the instrument not over three or four feet from the observer.

REFERENCE

1. INDUCTION-TYPE INDICATING INSTRUMENTS, **Paul MacGahan.** AIEE TRANSACTIONS, volume 31, 1912, pages 1565–77; discussion 1599–1608.

A. J. Corson: Mr. MacGahan has directed attention to his early interest in long-scale indicating instruments, and specific reference is made to his paper "Induction-Type Indicating Instruments" presented in 1912. In our examination of prior art, this reference was overlooked, but it will be promptly added as he suggests.

The remainder of his discussion originates the interesting question—how shall a development of this character be evaluated? Firstly, the design methods and mechanisms disclosed in the paper are perfectly general in character, and their application to instruments of a $4^{1}/_{4}$-inch rectangular dimension is, in a sense, incidental. Opinions as to the importance of this particular size of instrument may differ. We believe it to be of considerable moment as a contribution to more compact switchgear and better instrument grouping on panels. From the designer's point of view, however, the essential point is the extension of the scale length from 90 to 240 angular degrees, resulting in an approximate two to one gain in terms of linear measure, in any instrument of given size.

The question then resolves itself into a determination of the added value of this long scale. Firstly, both general considerations and test results show that long scales are observed more accurately than short ones. This gain in visual accuracy occurs twice, once in calibration and once in the observer's reading. Secondly, errors due to side motion of the moving system are reduced, as has been pointed out. In addition, the moving system is balanced with greater precision in terms of scale length. These and similar considerations have led to the almost universal adoption of long angular scales in such nonelectrical instruments as temperature indicators, mechanical tachometers, pressure indicators, and liquid-level gauges. While the application of long-range indication to electrical measuring instruments is admittedly more difficult, this would hardly appear to constitute valid grounds for attacking the desirability of the long scale as such, particularly when it is demonstrated that the design problems can be adequately solved.

The foregoing discussion assumes long-range indication to be obtainable without loss in factors which determine accuracy. An examination of the performance data shows this assumption to be generally true. Mr. Wilson's discussion is directed principally to these performance characteristics and brings out interesting points relative to torque to weight ratios, shielding, and damping. The various instruments have been designed to meet, and do meet, existing standards of performance, an accomplishment which was made possible by adhering to the fundamental types of mechanisms for which these standards were established.

The Fundamentals of Industrial Distribution Systems

Discussion and author's closure of paper 42-21 by D. L. Beeman and R. H. Kaufmann, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 272–9.

H. G. Barnett (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As a part of the discussion of the basic factors involved in distributing power in an industrial plant, Mr. Beeman and Mr. Kaufmann have made a very interesting cost analysis of a particular industrial plant. This analysis is specially interesting to me because of a large number of similar studies which I have helped to make both before and after Mr. Powel and I proposed the industrial-plant network idea before the Institute at the last winter convention (AIEE TRANSACTIONS, volume 60, 1941, April section, pages 154–6).

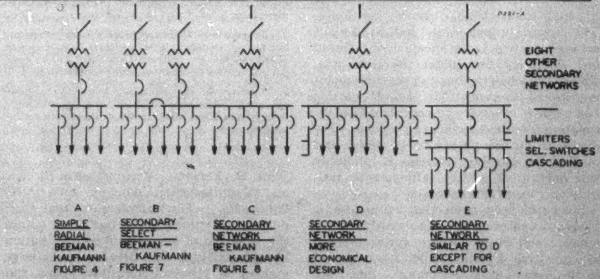
These studies covered a wide variety of plants. The load densities ranged from about 5 to 25 volt-amperes per square foot. All commonly used utilization voltages (120/208, 220, and 440 volts) were involved. Total loads ranged from 1,000 to 10,000 kva.

The results of these studies do not justify the conclusion that any one type of system can be expected to adequately meet the requirements in a large percentage of cases, although the authors have claimed that as the purpose of their analysis of a particular case. The wide variations, among actual plants, of voltage and arrangement of the incoming supply circuits, plant layout, utilization voltage, type of loads, and other factors make it impossible to use any one system in more than a minority of cases.

These studies do not confirm the wide difference of cost between the secondary network and the secondary selective and simple radial systems that is shown by Mr. Beeman and Mr. Kaufmann. Therefore, we have made a study of the plant described in the paper using the same loads, voltages, and requirements set forth in the paper. Cost estimates were based on using drawout circuit breakers in the 440-volt switchgear units. The relative costs shown by this analysis are shown in Table *A* along with the corresponding costs found by Mr. Beeman and Mr. Kaufmann. We have been able to check the authors very closely in the case of the secondary-selective system when the simple radial system is assumed to cost 100 per cent. However, our analysis shows a cost of 168 per cent for the secondary network system used by Mr. Beeman and Mr. Kaufmann which, according to their analysis, costs 175 to 200 per cent of the cost of the simple radial system. By using four transformer and switchgear units instead of six and using the same type of equipment used in the six unit network, the cost of the network is reduced to 156 per cent instead of 168 per cent of the cost of the simple radial system. By making use of cascading, which is sometimes advocated, the cost is further reduced to 150 per cent.

If it is assumed that the plant studied by the authors, because of reliability, flexibility, or other factors, requires a better system than the simple radial, the network becomes economically comparable to the other systems that can be used. For example, if the cost of the secondary-selective

Table A



	A	B	C	D	E		
BEEMAN KAUFMANN STUDY	100	145-150	175-200				%
		100-103	121-138				%
CHECK STUDY	100	143	168	156	150		%
		100	118	109	105	117-108	%

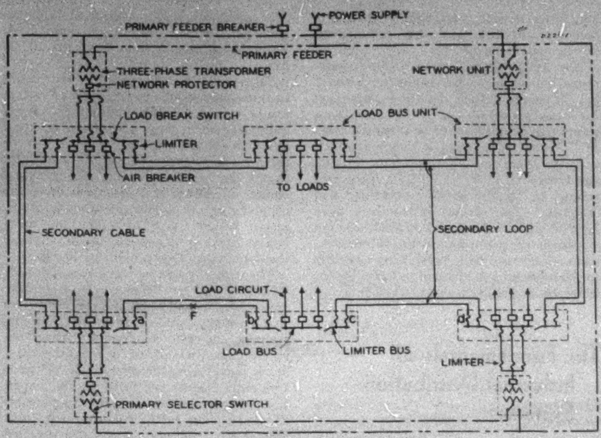


Figure 1

system is taken as the basis of comparison, the cost of the secondary network system ranges from 105 to 118 per cent as shown in Table *A*. The cost of 118 per cent is for the network system used by the authors for their comparisons. Ten other arrangements of the network system using limiters, primary selector switches, cascading, as well as full interrupting capacity breakers in various combinations, prove to be less expensive than the network chosen by the authors. The difference in cost between a network and the secondary-selective system is so small that the two can be compared, for practical purposes, on the basis of their respective operating characteristics.

All the comparative costs shown in Table *A* are based on using drawout breakers in the switchgear assemblies. If fixed breakers are used, the total cost is reduced in all cases but the relative costs are essentially the same as shown by the per cent figures shown in Table *A*.

The authors of the paper have expressed some doubt as to the limiter being a first-rate protective device. A thorough understanding of the characteristics of the limiter and its application should remove this doubt. The secondary loop in the industrial-plant network system is, in effect, a distributed bus extending throughout the load area. This extension of the bus and the consequent increase of the probability of faults on this bus make it necessary to provide means for isolating a faulted section of the loop. The device to do this should be as simple and reliable as possible, because it probably will operate only very infrequently, possibly once every several years.

Since the loop protective equipment is likely to operate very infrequently, maintenance and inspection are apt to be neglected. Lack of maintenance is no hazard to the operation of the limiter. Since the protective device operates only for a fault in the loop circuit, replacing a blown limiter does not materially increase the time required to restore the faulted circuit to operation, because the damaged conductors must be repaired at the same time. Immediate reclosure of the circuit is not even desirable. Therefore, the limiter presents no disadvantage and is actually somewhat more reliable than a breaker. The reason for using limiters in the loop is not a matter of cost but a question of basic system operation.

The correct operation of the loop protective devices depends on the use of parallel conductors in the loop as shown in Figure 1. When parallel circuits are used, a fault such as at *F* in Figure 1 causes only the limiters *a* and *b* to blow. If a single conductor per phase were used, limiters *c* or *d* or both would blow instead of *b* with the result that service from one load bus would be interrupted. If a load bus is established only at transformer locations, the load interruption would be less likely to happen. However, if a transformer should be disconnected from the loop for some reason, the same undesirable situation would exist as described in connection with Figure 1.

Three points will summarize this discussion.

1. Experience shows that no one system can be expected to adequately meet all the requirements in more than a small part of actual cases.

2. Whenever reliability, flexibility, or other operating factors require a better system than a simple radial scheme, the network frequently will be comparable in cost to other systems that can be used.

3. Limiters in the secondary loop have a basic operating advantage over the use of breakers; reduction of cost is not the fundamental reason for using limiters.

John S. Parsons (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors state that their purpose in writing this paper is to obtain the adoption of one system of distribution for industrial plants. Because of the large number of types of industrial plants and the many requirements and conditions peculiar to the various types, the use of any one type of distribution system in all of them is fundamentally unsound and impractical. We might just as well say that one type of distribution system should be used in the commercial areas and the residential areas of all cities and towns, and in all rural areas. As a result of the large amount of work we have done in connection with distributing power in industrial plants, one general conclusion can be definitely drawn. There is no one type of distribution system which should be used in all plants.

Also, as a result of our work on this subject, we have come to the conclusion that the simple radial is the cheapest system which can be used, and the secondary network is the best system, but it is by no means universally applicable. During the last year we have made distribution studies for more than 60 specific plants. As a result of these studies we recommended the secondary network system for about 60 per cent of these plants and some form of radial system for the others. Network systems are now in operation or being installed in about 25 per cent of the plants studied.

A paper dealing with the fundamentals of industrial distribution systems should cover all of the major forms of systems which are in use. The authors, however, in a fundamental paper on the subject have completely ignored the primary-selective network and spot network systems. These two forms of the secondary network system have been used more extensively in industrial plants than has the simple network system covered in the paper. The primary-selective and spot network systems offer practically all of the advantages of the simple network system, and usually at lower cost.

In comparing those forms of distribution systems covered by the paper, the authors place much emphasis on initial installed cost. We have found that for those plants where the simple radial system does not provide sufficiently reliable service, and there are very many such plants, we are in almost all cases talking about an initial cost difference of from 5 to 20 per cent between those systems which might satisfactorily be used. The cost of a plant distribution system will generally represent 10 per cent or less of the total investment in the plant. We are, therefore, talking about a difference in plant investment of approximately 0.5 to 2 per cent. Initial installed cost certainly should be considered when comparing plant distribution systems; however, it should not be over emphasized.

There are many factors which have a bearing on the installed cost of a distribution system. One such important factor is the matter of diversity. Yet diversity has not been discussed in this fundamental paper. When compared with a plant where no diversity between load centers is assumed, that is, a diversity factor of one is assumed, a plant having an appreciable diversity between load centers, such as a shipbuilding plant for example, will result in a higher installed cost for the radial systems and spot network and a lower installed cost for the simple network and primary-selective network systems. Even in those plants where the diversity is small or unknown at the time the distribution system is designed and is not considered in the design of the system, diversity will be reflected in better regulation and lower losses plus ability to carry more load if a simple or primary-selective network system is used. We have found that diversity and other factors peculiar to specific plants may cause the relative costs given in the paper to vary to the point where a secondary network system is less costly than the secondary-selective radial system.

When considering the cost of different forms of distribution systems, initial installed cost is not the whole story. The cost of meeting growing and changing load conditions over a period of years should be considered. This is of particular importance today, because the plants being built and revamped to turn out defense material will, when the war is over, require considerable modification to fit them for peacetime production. The flexibility of the simple and primary-selective network systems enables them to meet growing and changing load conditions at considerable less cost than do the radial systems and particularly the secondary-selective radial system.

The operating and maintenance difficulties with the secondary network system as discussed in the paper are, in our opinion, greatly exaggerated. This opinion is based on experience with network systems both in industrial plants and in a large number of small cities throughout the country. The great majority of operators we have talked to would rather operate a network system than a radial system. It certainly does not require a crew of supermen to operate and maintain a network system.

Our years of experience with all forms of distribution systems have taught us the danger of drawing general conclusions from one or even a number of distribution studies. Any industrial plant of appreciable size should be specifically studied and the form of distribution system which best meets its individual requirements should be used, whether it be network or radial or a combination of the two. The decision as to the proper system to use should not be made on the basis of generalizations based on the study of one or several specific plants, unless they are practically duplicates of the one under consideration.

Charles P. West (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have analyzed all the commonly used distribution systems for low-voltage power in industrial plants. Comparisons are made on the basis of cost, performance, reliability, and other factors. They recommend the secondary-selective circuit arrangement (Figure 7 in their paper) above all others and summarize their findings under five headings. The following comments are offered on their conclusions.

1. Installed costs are compared on the basis of the secondary-selective system being 150 per cent of the simple radial system base and the network system 200 per cent. Cascaded breakers are used to justify 25,000-ampere interrupting-capacity breakers for the former while 50,000-ampere breakers are specified for the latter. Cascading also can often be used here to reduce the size of the breakers. The network system is also subject to modification in a similar manner to that discussed for the secondary selective system, if parts of the areas supplied can sacrifice power for short intervals. This would tend to equalize the cost of the two systems.

2. The network system circuits shown by the authors do not show loop load-break isolating switches. These should be recommended, and when used, the loop circuits can be isolated for maintenance. A network system with these switches is as safe as the selective arrangement which is advocated, because there is no more necessity for working on live apparatus. In all cases it should be assumed that first-grade equipment will be applied, not "second-rate."

3. The authors concede "the highest order of service reliability" to the network system. It surpassed their recommended system in this respect. The fundamental network arrangement in the power supply and tie circuits unquestionably permits clearing faults with less disturbance to the continuous operation of a plant than any other system.

4. A network system engineered so that current distributes properly is no more difficult to operate than a selective circuit arrangement. This problem exists in any electrical network, and no difficulties have been experienced in the systems which are operating.

5. Voltage regulation is conceded to be good in both systems.

On the whole, the network system has the advantage in the important third point, reliability, and does not suffer by comparison in the others.

H. L. Rawlins and **P. O. Langguth** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. The authors have presented an interesting analysis of industrial distribution systems wherein four types of distributed load-center arrangements are described. As a result of this comparison, they have recommended a secondary-selective scheme of distribution.

2. To clarify this situation, we are adding a form of distributed load-center system, which consists of the secondary network with limiters. This arrangement will have costs comparable to the secondary-selective circuit arrangement with a service reliability rating of *A*. Safety requirements are completely fulfilled by the use of a knife switch or load-break switch between the cable limiters and the load bus. The particular section of faulted cable and limiters can be serviced dead with no interruption of service by simply opening the switch at each end of the involved cable section. Similar results can also be obtained by the use of disconnecting-type limiters.

3. The necessity for an indicator on limiters has not yet become apparent. There are several convenient means of determining if an open circuit exists in a cable run. Occasional check with a clip-on ammeter has been employed satisfactorily on network systems. No problem, however, is presented if an indicator should be required, because suitable indicators in various forms have been available over a period of years.

4. We note that Messrs. Beeman and Kaufmann describe the limiter as "second-rate apparatus." We might mention that a fusible element which functions directly by heat generated within itself is recognized as the most reliable of all over-current devices. This principle is fundamental and has been used over a long period of years in various forms with complete success.

5. There is a new development in this picture which has not yet been disclosed to the Institute. It consists of a new high-current interrupting device which simplifies the use of a network distribution system. This is a noteworthy development and places an entirely different complexion on the situation.

6. Briefly, this limiter is totally enclosed to prevent external demonstration of the device when clearing a fault. It has repeatedly interrupted currents in excess of 50,000 amperes at 600 volts with no perceptible noise or visible demonstration. A typical oscillogram is illustrated here of an interruption of 52,000 amperes with a restored voltage of 614 volts. The extremely short arcing time should be noted. Numerous tests in the high-power laboratory over a large range of currents have demonstrated the dependability of this limiter both in respect to time-current characteristics and its ability to interrupt any current within its rating.

Figure 2

7. The authors of this paper have stated by implication that the secondary network systems utilizing limiters lack reliability and are unsafe. We who have worked with this new form of 600-volt totally enclosed limiter, and who have seen it work repeatedly under exhaustive tests, are confident that the secondary network system utilizing such devices will normally be favored in industrial distribution systems.

E. M. Hunter and **J. C. Page** (General Electric Company, Schenectady, N. Y.): In laying out low-voltage a-c systems, frequently the two most difficult points to be settled are:

1. The basic type of system.
2. The general class of equipment to be used in the step-down substations.

Once these two important general points have been decided upon, the remaining details can usually be worked out easily. It is with respect to these two items that this paper by Messrs. Beeman and Kaufmann should prove valuable to persons engaged in laying out low-voltage a-c systems for industrial plants.

It is interesting to note that a system cannot be judged fairly unless both the basic circuit arrangement and the general class of equipment used in the circuit are judged simultaneously. For example, a low-voltage network, with open-type bus work or wiring, circuit breakers of inadequate interrupting rating, or fused knife switches, is probably considerably less reliable than a simple radial system incorporating properly insulated wiring, metal-enclosed load-center unit substations, and with circuit breakers of adequate interrupting rating.

Attention is called to Table I of the paper. The simple radial system in the tabulation is given a triple-*A* rating, and is marked down only on "service reliability." It will be of interest to see how this service reliability rating can be improved. The basic principle of load-center distribution is to divide the entire factory load area into small areas, each with its own substation. In some of the defense plants that have been installed, as many as 30 load-center unit substations have been used. Each of these units have been made alike in kilovolt-ampere rating, and so on, so that one spare load-center unit kept in the store room provides the means of restoring service to any area and without additional investment in spare capacity in each of the units. Sometimes these spare units are made portable. From this it is apparent that the simple radial type of system need not be condemned too severely for poor service reliability if the proper precautions are taken.

The network type of system is the highest in cost and has an *A* rating which is the highest in service reliability. It is the opinion of the discussers that the network type of system can be justified only when the ultimate in service reliability is required. The high cost of a network results, at least in part, in the fact that the secondary circuit breakers must have higher interrupting capacities than those used on the other three types of systems. Cost reductions obtained by the use of devices of inadequate interrupting capacities on the secondary of the network have been proposed in some applications. These methods, of course, lower the service reliability rating of the network, and it appears that they have no place in the network system.

A. E. Anderson (General Electric Company, Philadelphia, Pa.): The items of safety to operating personnel and continuity of service have been referred to in the paper. These, with other factors, are of considerable importance, particularly under present conditions, when such great importance is being placed on the nation's manufacturing facilities.

The use of circuit breakers in the high-voltage sides of the transformers in place of disconnecting switches as shown in the various schemes, Figures 2, 4, 5, 7, and 8 of the paper, would make possible a safer switching scheme and one more desirable from an operating standpoint. Along with protective relays it would provide additional protective and operating advantages.

At one time the value of short-circuit current obtainable from a low-voltage a-c system was generally considered as limited, regardless of system capacity. Fairly recent investigations have shown that these currents can be readily calculated, and that the calculated and measured values agree within usual engineering tolerances. Thus, the procedure has changed from something rather vague and mysterious to something that is rational, straightforward, and easy to apply.

The authors have recognized the latter procedure and have provided air circuit breakers of adequate interrupting rating for the various low-voltage circuits. It is further recognized that heavy short-circuit currents are to be expected if the voltage regulation is to be held to low values. While the above consideration points to circuit breakers of higher interrupting rating, there are two major factors that tend to counteract this tendency, without a sacrifice of other desirable operating features. One is the smaller distributed load center, with its saving in I^2R losses and flexibility, as compared with a single large substation. The second factor is the use of circuit breakers in cascade, which permits the use of a breaker, of interrupting rating less than that corresponding to the current obtainable at the particular location, provided it is backed up by a properly co-ordinated breaker, whose interrupting rating is equal to or greater than the available short-circuit current. No longer is it necessary to make a haphazard selection of circuit breakers for these low-voltage systems, with the possibility of running considerable risk of damage to the breaker and associated equipment, in the event of a short circuit near the outgoing terminals of the circuit breaker. It is quite important that breakers of adequate interrupting capacity be used. This means feeder breakers capable of interrupting the maximum short-circuit currents of the circuits in which they are connected, or feeder breakers of somewhat less interrupting rating in cascade with backup breakers of full interrupting rating.

The authors have included a number of pertinent items relating to the use of limiters, or limiters and isolating switches. In many applications the introduction of limiters will raise the short-circuit requirements of the feeder breakers, since they do not permit the cascade connection to be employed. In other words, the time of operation of the limiters with which the discusser is familiar is so great in relation to that of the modern feeder breaker, that it cannot give the desired degree of backup protection. Accordingly, the interrupting rating of the feeder breakers must be increased from some smaller value, permitted by the cascade connection, to a rating at least equal to the available short-circuit current. Consequently the introduction of limiters will, for most applications, increase the size and cost of the feeder breakers.

W. J. McLachlan (General Electric Company, Schenectady, N. Y.): The one most important conclusion drawn from the Beeman-Kaufmann paper is that, while some form of load-center distribution system usually will be found superior for plant supply, no one form of system is universally applicable. The best form for a given case depends principally on *cost* versus the value of *service continuity*, and the selection of the best form requires a rational evaluation of these items. It is to be hoped that the availability of this paper will encourage such rational evaluations, evidence of which has been lacking in an unfortunately large number of cases in the immediate past.

In evaluating the relative *costs*, it is obviously desirable that the forms of systems be costed on a comparable basis. The one item of greatest discrepancy in past comparisons has been the selection of low-voltage breaker interrupting capacities. Systems of one type, with breakers selected with interrupting capacities at least equal to calculated short-circuit currents, have been compared with another with the interrupting capacities grossly below calculated short-circuit values. Investigations, which have shown that calculated currents can be obtained in low-voltage circuits, point to the desirability of selecting interrupting capacities which are not below calculated values. However, if one elects to take the risk of applying lower rated breakers in one system, all logic requires that the same degree of risk be accepted in selecting the breakers for all forms of systems, if the cost comparison is to be equitable and fair.

When evaluating *service continuity*, it is essential that the over-all continuity of the system be considered. The form of load-center system selected can affect only part of the possible service outages. The total power outages result from failures in the following portions of the system:

1. Transmission supply to the plant.
2. Plant-supply stepdown substation.
3. Plant-distribution primary feeders.
4. Load-center unit transformers.
5. Low-voltage feeders from load-center units to individual load areas.
6. Laterals to individual loads and individual load and control equipment.

In general, only those outages due to failures in the plant-distribution primary feeders and load-center unit transformers are influenced by the form of load-center system selected. If the majority of the outages are expected to be due to failures in the other parts of the system, the total outages can be influenced by but a small degree by the form selected and differences in reliability of the types of systems described are of proportionately small significance. As an example, several applications of the more expensive secondary-network system have come to attention in plants supplied by only one long high-voltage transmission circuit. In such cases, the probability of failures of the entire plant supply is several times that of failures in the plant-distribution primary feeders, making it appear that little effective benefit will be derived from the added cost and operating complication of the network system.

On the other hand, if the expected interruptions in the primary supply system are rare, which presupposes at least duplicate transmission sources, and probably dual supply substations, the service reliability of the distribution system takes on increased significance. Even then, it should be remembered that, regardless of the form of load-center system selected, the outages due to failures in the low-voltage feeders and laterals and in the individual load and control equipment will be unaffected by the selection made.

These comments are made to encourage rational evaluation in the selection of the system to be employed in specific instances. It is believed that this desirable condition will be considerably enhanced by the availability of the comparative data in the Beeman-Kaufmann paper.

R. H. Kaufmann: It is indeed gratifying to note the interest which is displayed in the subject of industrial power distribution. It is indicative of an increasing consciousness of the importance of this phase of the industrial electrical problem which is the first step leading toward a more general recognition of the important part which the distribution system plays in the performance of electrified industry.

The debatable issues which have been raised in the several interesting and valuable discussions center largely on the selection of the particular form of load-center distribution to be used, and more specifically a consideration of the secondary-selective system versus the secondary network.

The secondary-selective system has a distinct advantage in respect to electrical simplicity and safety of operation. Each main low-voltage bus section has access to two independent power-supply channels which insures reliability of service. The secondary-selective system obviously will not be used to the complete exclusion of all others. A very considerable number of present industrial systems are adopting the straight radial form. The primary-selective system will find application in some instances, particularly for load areas involving scattered independent buildings each requiring only a small amount of power which will hardly justify more than a single load-center unit. The secondary network system will likewise find fields of application, particularly for continuous process operations in which even a momentary loss of voltage would result in large consequential losses either in the form of damage to productive machinery or spoilage of large quantities of material.

VARIATION IN SYSTEM FORM

It has been intimated that the form of secondary-network system illustrated in Figure 8 of the basic paper fails to realize as good an over-all economy as can be obtained with other variations.

Mr. Barnett, for instance, suggests the incorporation of the primary selective feature as illustrated in Figure 1 of his discussion. The fundamental difference lies in the use of two primary feeders instead of three and the addition of primary transfer switching equipment at each load-center unit. Relative to the straight radial system as in Table I of the basic paper, the primary selective-secondary network system shows a first cost level of 185 per cent as compared with 175 per cent for the system illustrated in Figure 8 with no improvement in other characteristics. In fact, some definite disadvantages must be accepted:

1. Both primary feeders must come in close proximity at each load center, which provides a number of vulnerable spots at which a disturbance could result in a complete power outage for the entire area.
2. Operating hazards as described in the primary-selective system would likewise be accepted.
3. This system depends for its merit on the prompt transfer of units to the healthy feeder following a feeder outage.
4. It is important that the primary-selective switches be alternately positioned under normal service conditions to avoid the connection of two or more adjacent stations to the same primary feeder. If this precaution is neglected, loss of power on that primary feeder would impose a very heavy transfer load in the tie circuit and in the next adjacent energized load-center unit.

The use of four rather than six substations, as suggested by Mr. Barnett, in either of these secondary network systems would fail to improve the over-all economy. The size of each station would be increased to 1,125 kva (6/4 of 750 kva) and short-circuit current levels on the low-voltage bus sections would exceed 50,000 amperes. A substantial increase in low-voltage switching-equipment cost would thus be incurred. The interconnecting low-voltage tie circuits would be of substantially the same total length but would require 50 per cent increased current-carrying capacity due to the greater load fed from one station.

The spot network represents a variation which closely resembles the secondary-selective system. It differs in that the spot network permanently parallels the two transformers on a common bus using network protectors in the low-voltage circuit from each transformer. The absence of interconnecting low-voltage tie circuits with remotely located stations (as in the secondary-selective system) considerably simplifies the problem for the operating department. The chief question to be considered in deciding between these two system forms is: Is there justification for the additional initial investment resulting from increased switching-equipment cost to obtain uninterrupted voltage in those few instances that power flow on one primary feeder is interrupted; or will service reliability requirements be adequately met by maintaining an alternate low-voltage power supply which can be tapped at a moment's notice merely by closing the low-voltage tie breaker between the complementary bus sections?

Cascade operation of low-voltage air circuit breakers in the secondary network system is regarded as contrary to the fundamental objective. The secondary network system distinguished itself by affording service continuity (uninterrupted service) as contrasted with service reliability (available service). A compromise in this respect would strip the secondary network of its chief advantage.

DIVERSITY FACTOR

Mr. Parsons in his discussion stresses diversity factor as an important influence on over-all cost of the secondary network system. Although it may have significant influence, yet to be able to take advantage of diversity factor between load-center substations, it is important that, at the time of heavy load on one particular station, the immediately adjacent stations be operating at subnormal load. It is of little benefit to have lightly loaded stations on the opposite side of the loop as they will contribute only slight support to the remotely located, heavily loaded section. The installation of cross tie circuits to improve this situation will likely defeat its own purpose as a result of increased tie-circuit cost and switching-equipment interrupting capacity and cost. Furthermore, the small advantage obtained affects only the transformer capacity. Primary feeder circuits together with switching equipment, low-voltage feeders together with switching equipment, and interconnecting tie circuits together with switching equipment are not benefited. Inasmuch as the transformers account for approximately 10 to 15 per cent of the cost of the over-all system under investigation, based on unity diversity factor, it must certainly be concluded that no substantial reduction in system cost could be realized as a result of diversity factor.

LIMITERS

For all comparisons reported in the paper, standard air circuit breakers have been used in all low-voltage circuits, including feeders and tie circuits. The standard air circuit breaker constitutes a recognized power-switching device with ratings and performance thoroughly covered by industry standards. The application of standard air circuit breakers within the limits defined by ratings provides the advantages:

1. Ability to repeatedly switch the circuit at will with assured safety.
2. Ability to safely and positively interrupt the maximum fault current.
3. Ability to quickly restore service following an interruption.
4. Reliable indication of operation and position. (Remote indication of circuit-breaker position is easily possible.)
5. Availability of electrical operation.

Messrs. Barnett, Rawlins, and Langguth have commented on the possible application of limiters located in the tie circuit of the secondary network system, in combination with a suitable switching device to provide the necessary degree of circuit control. In the discussion by Messrs. Rawlins and Langguth, reference is made to the limiter as a new development not yet publicly disclosed. This, of course, implies that it is still in the developmental stage and that the ultimate stabilized product will likely incorporate any improvements that installation and use indicate are needed.

In addition to the ability to interrupt fault currents safely, positively, and consistently, there are a number of other considerations. Thermal overload protection of circuit runs within the building should be provided. The thermal effect from heat normally liberated in the fusible unit must be recognized and cared for. Provision should be made for dissipating the heat so generated without subjecting the terminal section of the cable or low-voltage bus to elevated temperatures. The use of tie-circuit switching equipment which duplicates that used in the balance of the structure would be desirable.

OPERATION

In respect to operating problems of the several typical system forms, there can be no question that the secondary network system is excelled by the other three forms. Beyond that, the ratings in Table I have been assigned no quantitative significance.

That the secondary network system permits growing loads to be met with greater ease and less cost is a controversial contention. In addition to installation of the new substation with associated high-voltage supply and low-voltage feeder circuits, the secondary network system involves incorporation of the new station in the existing tie-circuit system. Unless tie circuits are properly designed, short-circuit current levels on existing stations may be elevated. The primary feeder arrangement must be reviewed to insure that power supply to two or more adjacent stations will not be simultaneously interrupted as a result of a single primary feeder outage.

A New Voltage-Regulating Relay Plus Line-Drop Compensator

Discussion of paper 42-32 by H. J. Carlin, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 63–6.

H. L. Prescott (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The new relay described in Mr. Carlin's paper has several features differing from previous voltage-regulating relays which affect the application of the relay to a step voltage regulator and it may be well to look at it from an application viewpoint.

This relay has a damping characteristic of importance on many circuits on which there are, almost continuously, small voltage changes which are not large enough to close the voltage-regulating relay contacts. The conventional type of relay follows these changes as they occur. The relay described in this paper, due to its inherent time delay, damps out these continual changes and operates smoothly, following the mean voltage. Many circuits also are subject to relatively large momentary voltage changes due, for example, to switching surges. The conventional relay will also follow these changes while the new relay will to a large extent damp them out. This damping characteristic, therefore, reduces the mechanical duty on the relay, reduces wear on the bearings, and increases the length of life of the mechanical parts.

A second feature of importance in applying this relay is its integrating or cumulative characteristic. In general, the older types of voltage-regulating relays are used to operate a time delay relay and in order to cause an operation of the step regulator, the voltage applied to the voltage-regulating relay must hold the relays contacts closed for the full timing period of the time delay relay. With the new voltage-regulating relay, the relay response depends not upon the voltage remaining outside the range of the relay setting continuously for a definite time but upon the difference in the time it is outside the range from the time it is within the range. Therefore, if the voltage is outside the range more of the time than it is inside the range, this voltage-regulating relay will close its contacts and cause the regulator to operate to bring the mean voltage inside the relay settings. This relay will hold the voltage closer to the desired value than will the older types of voltage-regulating relays.

A third feature of the new voltage-regulating relay is its differential or inverse time characteristic. When conditions on the power system cause relatively large voltage changes, it is desirable to have the voltage regulators operate more quickly than when the voltage changes are small. Figure 1 of the paper shows that the greater the voltage variation from the neutral value from which the relay is set, the shorter will be the time until the contacts close. The new relay, then, instead of having a constant time delay regardless of the magnitude of the voltage change as is the case with separate voltage-regulating and time delay relays, has a shorter time delay when large voltage corrections are required than it has when the voltage variation from normal is small.

The characteristics just described result in reducing contact maintenance. Since momentary voltage variations do not cause the voltage regulating relay to close its contacts, this relay closes its contacts after the time delay period rather than before the time delay period, and, therefore, the contacts close only when an operation of the regulator is actually to be made. The number of contact operations, therefore, is reduced to equal the number of tap-changer operations, resulting in less burning of the contacts than on the older type voltage-regulating relays whose contacts closed to initiate operation of the time delay relay. This feature also eliminates the necessity for separate compounding coils.

The line-drop compensator which is built into the relay described in this paper is different in both construction and principle of operation from the line-drop compensators which have previously been built into voltage-regulating relays. In the older relays, the line-drop compensator operation was based only on the magnitude of the current. In the new relay, the compensator operation is based not only on the magnitude of the current, but also upon its power factor and the impedance angle of the line. The new device, therefore, gives a closer approximation to the theoretically correct value than the older type. It will, therefore, be applicable in some cases where in the past separate voltage-regulating relay and line-drop compensator were used.

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